

Article

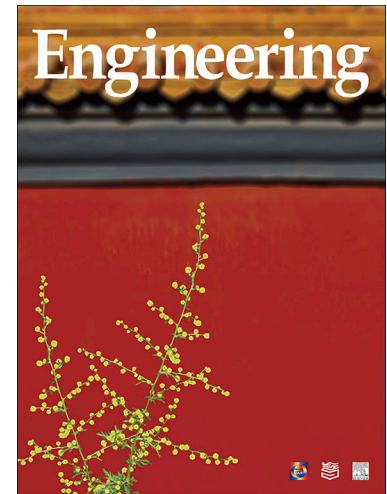
A New Web-Type Concept of Floating Photovoltaic Farms in Open Sea Environment

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Research

Large Offshore Floating Structures—Article

A New Web-Type Concept of Floating Photovoltaic Farms in Open Sea Environment

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Abstract

The rapid advancement of floating photovoltaic (FPV) technologies has led to increasing interest in their deployment in marine environments. However, the survivability of FPV systems under harsh oceanic conditions, particularly when subjected to large wave impact loads, remains a significant challenge. Addressing this issue necessitates a fundamental shift in design approach. Natural structures are well known for their exquisite design. For example, the silken webs of web-spinning spiders have evolved to span large areas and endure extreme weather conditions and impact loads, while utilizing minimal material. Such remarkable natural characteristics provide valuable inspiration for the next generation of FPV systems. In this study, we propose a nature-inspired design concept featuring a bio-inspired web-type floating structure to support FPV modules. While the design draws on the distributed and flexible features of web-like geometries, it does not seek to replicate the exact structure or biological function of a spider web. The technical feasibility of this innovative concept was evaluated using the Morison model. Various FPV web configurations were analyzed to evaluate the influence of environmental loads and key design parameters. The study compares motion responses and mooring load variations across various wave conditions. The results indicate that, for the proposed web-type structures, the rope connection can maintain the overall motion at a low level, and the peak mooring tensions can be optimized. The pretension on the connecting

mooring lines can be optimized by tuning the gap between the modules. Furthermore, the dynamic performance of the large FPV system was evaluated. This study presents an early-stage framework, employing the spider-web configuration as a structural analogy rather than a validated solution.

1. Introduction

Since 2016, the floating photovoltaic (FPV) energy sector has experienced rapid growth, achieving an installed capacity of 3 GW by 2020. Over 60 countries have joined the FPV initiative, with total capacity projections reaching 10–30 GW by 2030 [1]. However, the majority of technologies developed to date are tailored for calm, inland water areas, limiting their applicability in dynamic offshore environments [2]. Although several concepts for ocean-based FPV have been proposed, current technologies remain insufficiently developed to ensure reliable operation under harsh marine conditions. A significant advancement in design is urgently required to enable FPV systems to operate reliably and efficiently in offshore environments over extended periods.

Recently, modular solutions have garnered increasing attention within the ocean renewable energy sector. Modularized arrays offer significant advantages in terms of ease of installation, disassembly, and scalability [3,4]. These systems can be expanded to cover areas ranging from tens to thousands of meters, depending on specific deployment requirements. Furthermore, modular configurations can help mitigate hydroelastic structural issues compared with a single large structure [5]. Modular arrays are increasingly utilized in various engineering applications, including wave energy converter (WEC) arrays [6–9], aquaculture platforms [10], and floating solar panels [11,12], to mitigate motions and loads induced by waves, current, and wind. These systems often comprise a large number of modules to satisfy the dual requirements of power generation and the spatial needs of aquaculture operation [10,13].

The interconnections between floating modules are typically flexible, employing components such as hinges, ball joints [13,14], elastic elements [15], or rigid connectors [16]; hinges, in particular, enable rotational movements between modules while maintaining overall structural integrity. Several studies have focused on the motion responses and load distribution of hinged floating bodies under various wave conditions (WCs). Noad and Porter [17] compared device proportions, hinge position, and number of pontoons of an articulated raft WEC, concluding that positioning longer pontoons at the aft, relative to the direction of incoming waves, enhances the power performance of the system. A Pelamis WEC comprising four to five hinged tube segments was designed to harness wave energy through the rotational motion at the joints [8,9]; it demonstrated both high energy capture efficiency and robust structural performance under extreme WCs. Zhang et al. [18] investigated the dynamic behavior of large arrays composed of multiple hinged floaters and reported that hinge constraints significantly suppressed heave motions across the array, although pronounced pitch motions were observed in the presence of longer waves.

The use of hinged connectors for rigid modules is a preferred option for flexible FPV solutions [19]. For example, SolarDuck (the Netherlands) developed a triangular FPV module concept that employs flexible connections, allowing the structure to move with the waves and better accommodate wave-induced loads [11]. Wei et al. [20] investigated the motion characteristics of modularized floaters with hinge connections under wave action, identifying the ratio of structure length to wavelength as a key parameter influencing the heave and pitch responses of modular solar farms. Ji et al. [21] designed three types of connectors to integrate six floating modules into an FPV system; numerical simulations indicated that the ball joints aligned with the wave propagation direction deliver the best performance. Another approach to flexible FPV solutions involves the use of membranes to support photovoltaic (PV) modules, as demonstrated in the Ocean Sun (Norway) [22], Det Norske Veritas (DNV; Norway) [23], and Mining Innovation, Rehabilitation, and Applied Research Corporation (MIRARCO; Canada) projects [24]. Notably, the feasibility of these thin-film concepts remains uncertain, as prototype tests and pilot projects have revealed significant challenges and failures.

Although hinged or joint connectors for rigid modules have been widely explored for assembling large FPV arrays, these systems are generally classified as semi-flexible connections. These connections are particularly vulnerable to damage or failure when subjected to continuous wave impact and dynamic loading. In contrast, certain flexible connections—particularly those utilizing ropes—have attracted considerable interest owing to their capacity to absorb energy and more evenly redistribute loads across the structure. The flexible connection typically experiences lower torque and shear forces compared with conventional mechanical joints. Jiang et al. [15] conducted model tests on a scaled array comprising 3×2 modules interconnected by ropes under both regular and irregular WCs. Their findings demonstrated that, in long-wave scenarios, the motions of the six modules were highly consistent, with the relative positions between modules remaining stable. Wang et al. [25] further validated the experimental results of Jiang et al. [15] through numerical simulations of a

star-type FPV system linked by flexible connectors. Their study examined the effects of varying WCs and connector parameters on the overall performance of the FPV system. Similarly, Luo et al. [26] developed a four-module offshore FPV system concept utilizing soft rope connections. They conducted physical model tests and developed a corresponding numerical model, which was validated against experimental data. This research investigated the influence of factors, such as module draft, soft rope stiffness, and mooring stiffness, on system performance. Collectively, these foundational studies on flexibly connected FPV systems provide valuable references for further advancements in this field.

In this study, a novel web-type framework for FPV systems, as shown in

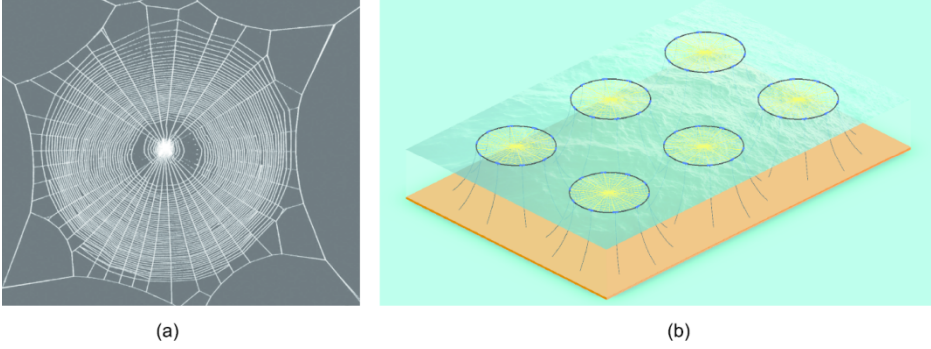


Fig. 1, is proposed. A coupled dynamic response analysis was conducted using multi-body hydrodynamics in conjunction with the lumped mass method.

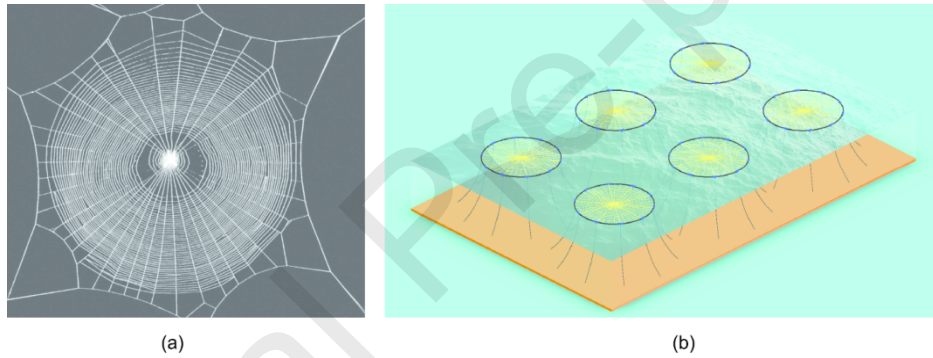


Fig. 1. (a) Geometry of a typical spider web; (b) web-type floating concept for FPV.

The remainder of this paper is organized as follows. Section 0 details the conceptual model of the proposed system. Section 0 outlines the time-domain motion equations, wave loading, and connection force formulation. Section 0 validates the experimental results. Section 0 explores the effects of various wave and design parameters on the modularized arrays in regular waves. Finally, Section 0 summarizes the key findings and contributions of this study.

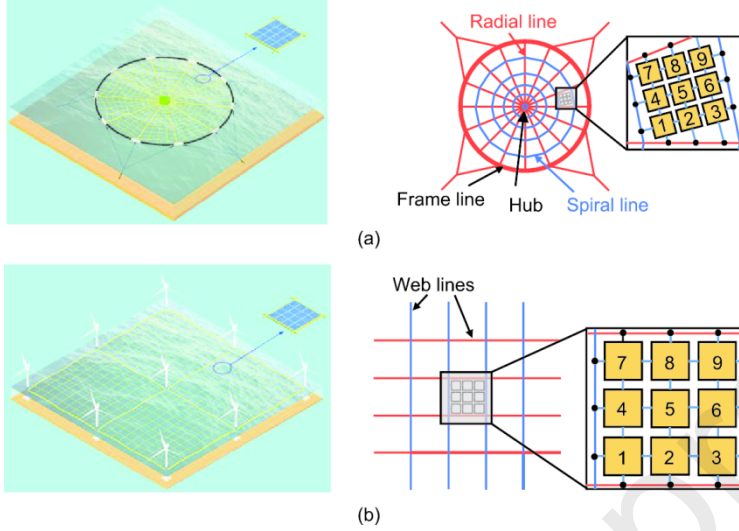
2. System description

This study was inspired by spider webs, with particular emphasis on the remarkable material properties of spider silk and mechanical advantages of web-like structural configurations [27]. Despite extensive research, a practical engineering application that fully leverages the unique and superior characteristics of spider web structures has yet to be realized. This study pioneers the exploration of a floating structure concept modeled on the flexibility and load-sharing capabilities inherent in spider webs. Specifically, the analysis focuses on 3×1 and 3×3 modular units, and introduces a global spider web-inspired concept as a conceptual extension of these local modules. Its behavior under wave loading remains unvalidated at this stage.

The proposed web-type framework features a fully flexible design, enabling the system to distribute both local impacts and global wave loads uniformly across the entire structure. This comprehensive load-sharing mechanism substantially mitigates the failure risks typically associated with mechanical joints. Additionally, the web-inspired framework demonstrates inherent resilience, maintaining operational integrity even in the event of partial rope failure, analogous to

the robustness observed in natural spider webs. Furthermore, compared with conventional modular systems connected by hinges, the web framework simplifies the installation and maintenance of auxiliary devices, resulting in greater cost-effectiveness. Furthermore, the entire system can be efficiently transported to installation sites by ship towing, thereby reducing transportation challenges.

The flexible web frame, comprising both spiral and radial lines, enables the system to deform with waves as a unified



structure, as shown in

Fig. 2(a). This structural deformation facilitates the absorption of wave energy through elastic response. The elastic ropes, fabricated from synthetic materials, exhibit time-dependent viscoelastic behavior influenced by both prior load history and the applied mean load. An alternative configuration, shown in

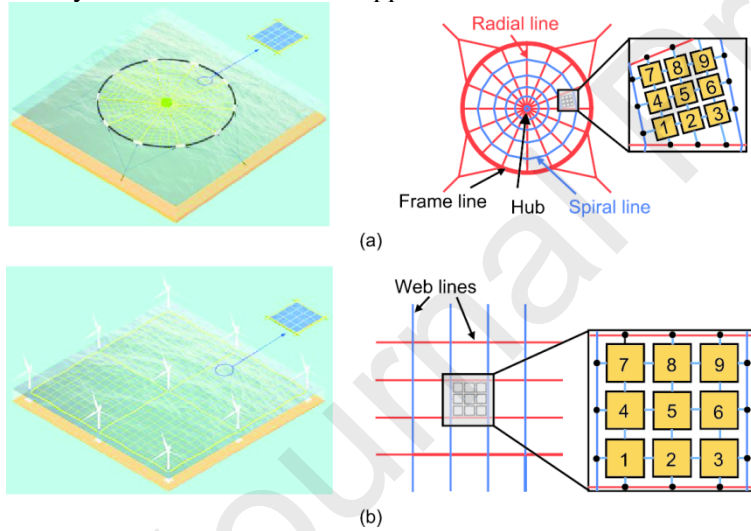


Fig. 2(b), demonstrates the potential for deploying the web structure between offshore wind turbine towers. These wind-turbine towers offer ideal support for the web, enabling effective space utilization in the offshore area between the turbines. In both designs, the modularized array can be considered as having an approximately square-shaped arrangement.

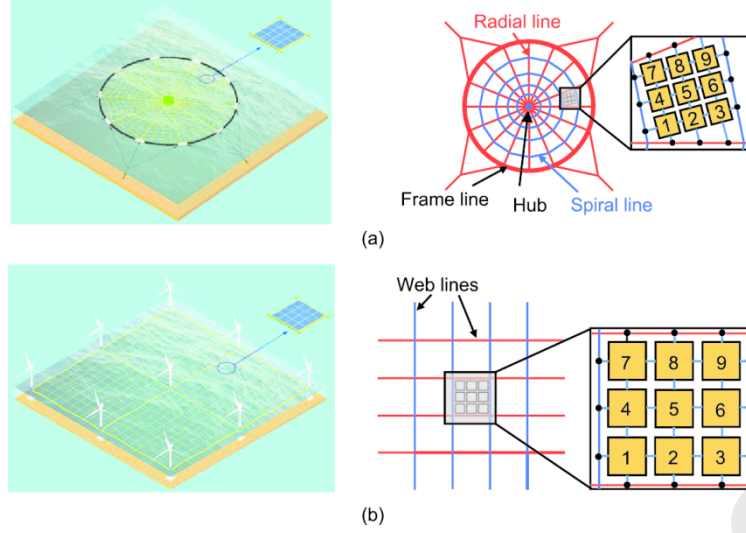


Fig. 2. Two web-type concepts for a floating solar plant. (a) Moored to the seabed, (b) moored to offshore wind turbine towers. The inset shows a 3×3 arrangement of floating modules, labeled 1–9, where each number denotes an individual module.

This study focuses on a simplified 3×3 matrix, as shown in Fig. 3, which is extracted from the proposed web-like FPV system. The outer lines are modeled as thick, pre-tensioned elements that function as rigid boundaries, permitting only vertical motion. The internal rope connections embody bio-inspired principles, such as adaptability, redundancy, and modular interaction. The 3×3 matrix can be integrated into a larger web-like FPV array, and a full-scale deployment with multiple substructures can be arranged within the spaces formed by intersecting radial and spiral lines.

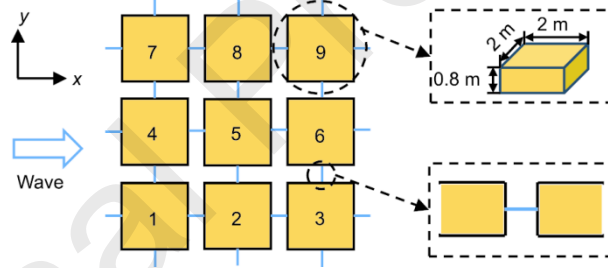


Fig. 3. Configuration of a typical 3×3 FPV array in solar webs.

The wave propagates along the positive x -axis direction, as shown in Fig. 3. Each module was defined within a body-fixed coordinate system (x, y, z), with the origin located at the center of the geometry of a single module. Each module substructure supporting the PV system comprises lightweight thermoplastic matrix composites, which are fully recyclable and offer high specific strength as well as excellent corrosion resistance—attributes that contribute to reduced life-cycle costs. Insights from sea trials of a WEC-PV hybrid system [28] indicate that slamming wave loads can easily damage solar panels if an air gap exists between the floater and panel. To mitigate this risk, the present study mounts the panels directly onto the top surface of the floaters. For modeling purposes, the substructures are represented as flat boxes with uniform mass distribution; relevant parameters are detailed in Table 1. In our numerical model, we assumed that each module was half-submerged and floating on the water surface, which is consistent with typical floating PV designs. In practice, recyclable materials with densities lower than that of water (e.g., $880\text{--}960\text{ kg}\cdot\text{m}^{-3}$ for polyethylene) are considered. Alternatively, floaters may be designed as hollow containers with ballast water tanks to adjust draft and density. Given that the cross-sectional dimensions were significantly smaller than their wavelengths, their hydrodynamic loads were estimated using the Morison equation for simplification.

Table 1

Physical properties of the individual module.

Variables	Full-scale value
Length (m)	2.0
Width (m)	2.0
Height (m)	0.8
Material density ($\text{kg}\cdot\text{m}^{-3}$)	512
Draft (m)	0.4
Gap between modules (m)	1.0
Total length/width of array (m)	10.0

The connection lines utilized in this study were composed of polyester ropes. As a synthetic material, polyester is immune to corrosion and demonstrates superior tension fatigue, out-of-plane loading, and torsion compared with steel components. Owing to its low density, polyester rope necessitates lighter connecting hardware and reduced structural bracing, thereby enabling a more efficient system design. The specific parameters of the polyester ropes utilized in this study are listed in **Error! Not a valid bookmark self-reference..**

Table 2

Physical properties of synthetic rope.

Variable	Full-scale value
Material density ($\text{kg}\cdot\text{m}^{-1}$)	1.65 (in water)
Diameter (mm)	38
Axial stiffness (kN)	4.39×10^3
Minimum breaking strength (MBS; kN)	219
Safe load (safety factor = 12) (kN)	18.2

The minimum breaking strength (MBS) represents the maximum load a rope can endure before failure. In contrast, the safe load denotes the maximum load that can be safely applied to the rope during normal operations, calculated by applying a safety factor of 12 to the MBS.

3. Methodology

A RIFLEX [29] model of the web-type platform was developed to analyze the load distribution on the ropes. RIFLEX (SINTEF Ocean, Norway) is a computational tool designed for linear and nonlinear analysis of flexible risers and other slender structures. In this model, vertical bar elements represent the rigid modules, whereas horizontal bar elements simulate the soft rope connection.

3.1. Rigid body motions

The modules in the floating array were modeled as rigid bodies. The 6-degree-of-freedom (DoF) dynamic equations for a floating rigid body are expressed as follows:

$$(\mathbf{M} + \boldsymbol{\mu}(\infty))\ddot{\boldsymbol{\eta}}(t) + \int_0^t \mathbf{h}_r(t - \tau)\dot{\boldsymbol{\eta}}(\tau)d\tau + \mathbf{K}\boldsymbol{\eta}(t) = \mathbf{f}_e(t) + \mathbf{f}_c(t) \quad (1)$$

where $\mathbf{M}$ represents the body mass matrix; $\mathbf{K}$ represents the restoring stiffness matrix of the floater; $\boldsymbol{\eta}$, $\dot{\boldsymbol{\eta}}$, and $\ddot{\boldsymbol{\eta}}$ represent the displacement, velocity, and acceleration vectors, respectively; and $\mathbf{f}_e$ represents the wave excitation force. This study considered only the effects of first-order wave loads. $\mathbf{f}_c$ represents the restoring force of the connection rope. The hydrodynamic coefficients and external forces were described in body-fixed coordinates. t represents the current time and τ is the dummy variable of integration, representing a past time used in the convolution integral to model memory effects of radiation forces. $\boldsymbol{\mu}(\infty)$ represents the added mass matrix at infinite frequency and $\mathbf{h}_r$ represents the retardation function matrix derived from Cummins' equation, which can be represented as follows:

$$\mathbf{h}_r(t) = \frac{2}{\pi} \int_0^\infty \omega(\mathbf{m} - \boldsymbol{\mu}_r(\omega)) \sin(\omega t) d\omega = \frac{2}{\pi} \int_0^\infty \boldsymbol{\lambda}_r(\omega) \cos(\omega t) d\omega \quad (2)$$

where ω represents the wave frequency; $\mathbf{m}$ represents the added mass matrix at infinite frequency; $\boldsymbol{\mu}_r(\omega)$ represents the added mass in frequency domain; and $\boldsymbol{\lambda}_r(\omega)$ represents the wave radiation damping in the frequency domain.

When hydrodynamic interactions such as radiation and diffraction are neglected, Morison's equation provides an effective means of estimating wave loads on each floater. The floating element shown in Fig. 3 is modeled as an assembly of slender bodies, enabling the application of Morison's equation to calculate the wave load on each module. This approach is particularly appropriate when the characteristic diameter of the structure is small relative to the incident wavelength, typically when the diameter-to-wavelength ratio D/L is less than 0.2. Under these conditions, the structure does not significantly disturb the surrounding flow, and the convolutional term on the left-hand side of Eq. (1) can be omitted. The wave load on each module can be calculated as

$$\mathbf{f}_e(t) = \frac{1}{2} \rho C_D A_0 \mathbf{u}|\mathbf{u}| + \rho(1 + C_A)V \frac{d\mathbf{u}}{dt} \quad (3)$$

where ρ represents the fluid density, C_D represents the drag coefficient, A_0 represents the projected area, $\mathbf{u}$ represents the flow velocity, C_A represents the added mass coefficient, V represents the displaced volume, and $\frac{d\mathbf{u}}{dt}$ represents the acceleration of the flow. The values of C_D and C_A for square cross-sections can be selected according to the guidelines

provided in DNVGL-RP-C205 [30]. In this model, radiation interactions between bodies were ignored to enhance computational efficiency.

3.2. Lumped mass method

Each floating module experiences both hydrodynamic loads and restoring forces generated by the web ropes, which serve to mitigate excessive motion responses under environmental loading. In this study, the web ropes and mooring line system are modeled using a three-dimensional lumped mass method.

As shown in Fig. 4, the lumped mass method calculates the tension within lines by dividing them into multiple lumped mass points connected by massless elastic elements. All forces acting on the line are assumed to be concentrated at these lumped mass points. The line was divided into n segments, with one end of the first segment attached to Body 1 and the opposite end of the n th segment connected to Body 2. The line is subjected to external forces, including gravity, buoyancy, added mass, and damping forces, all of which are assumed to act on the nodes along the line. The mass of each segment was evenly distributed between the nodes at both ends. The three-dimensional equations of motion for the mooring line are solved using the initial conditions specified at each point along with the displacement boundary conditions applied at both ends of the line.

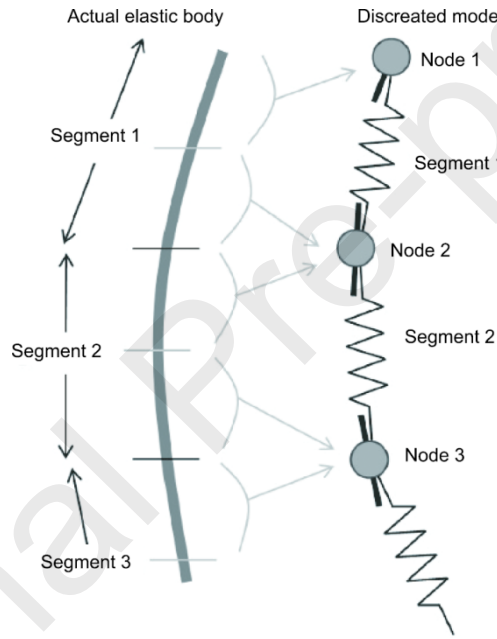


Fig. 4. Model of lumped mass method.

Both mooring lines and ropes were modeled as bar elements within RIFLEX. The spatial bar element utilizes a total Lagrangian formulation, based on integrated cross-sectional forces and small-strain theory. The element was assumed to be straight with an initial cross-sectional area A_0 along its length. The two nodes of each element possess three translational DoFs, directly in the global coordinate system. The element lengths were denoted l_0 and l in the initial and deformed configurations, respectively. The deformed element length is expressed as

$$l = \sqrt{\Delta x^2 + \Delta y^2 + \Delta z^2} \quad (4)$$

where Δx , Δy , and Δz represent the elongations of the element in the x -, y -, and z -direction.

When the small-strain theory is applied, the axial force of the element is expressed as

$$f_c = \frac{l - l_0}{l_0} (EA) \quad (5)$$

where EA represents the axial stiffness, E represents the Young's modulus of the material, A represents the cross-sectional area, and f_c denotes the axial force in a single connection element. The strain ε is expressed as follows:

$$\varepsilon = \frac{l - l_0}{l_0} \quad (6)$$

The current modeling approach assumes dry-state properties of viscoelastic connectors. These materials demonstrate different behaviors when submerged, including increased compliance and dimensional changes. This limitation needs to be addressed in future studies.

3.3. Pretension and clump weight

In configurations where floaters are connected by soft links, preventing collisions is essential to avoid structural damage and maintain system stability. The clump weight was designed to provide pretension to the rope, eliminating the need for a dedicated tensioning device. This passive tensioning strategy enhances system reliability and simplifies installation, which is particularly advantageous in offshore environments where space for winches is limited.

The relationship between the weight and pretension satisfies the Pythagorean theorem, as shown in

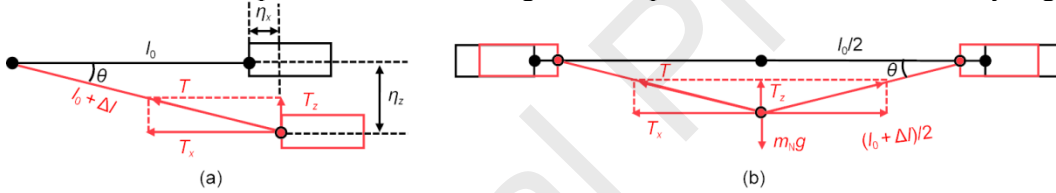


Fig. 5. The lumped mass was positioned at the midpoint of the rope to ensure uniform distribution of tension.

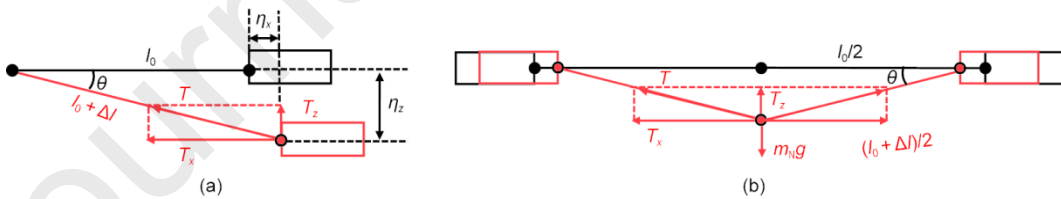
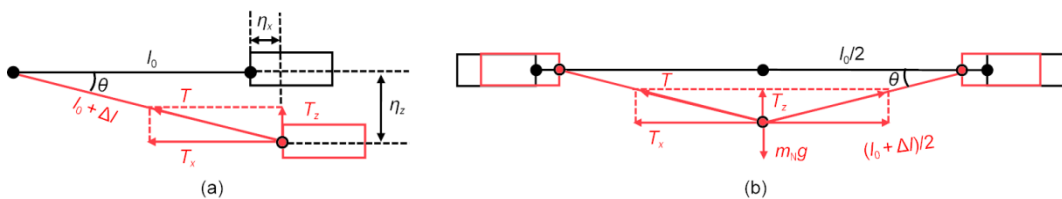


Fig. 5. Relationship between (a) tension and displacement, and (b) pretension and mass of the clump weight at the equilibrium position. η_x, η_z : horizontal and vertical displacements, respectively; T_x, T_z : horizontal and vertical components of the tension force T , respectively; θ : inclination angle of the rope element; m_N : lumped mass concentrated at the node; g : gravitational acceleration; Δl : elongation of the element which is the difference between its deformed and original length.



As shown in

Fig. 5(a), the tension–displacement relationship in both the horizontal and vertical directions can be expressed as follows:

$$T_x = \frac{EA}{l_0} \frac{(\sqrt{(l_0 + \eta_x)^2 + \eta_z^2} - l_0)(l_0 + \eta_x)}{\sqrt{(l_0 + \eta_x)^2 + \eta_z^2}} \quad (7)$$

$$T_z = \frac{EA}{l_0} \frac{(\sqrt{(l_0 + \eta_x)^2 + \eta_z^2} - l_0)\eta_z}{\sqrt{(l_0 + \eta_x)^2 + \eta_z^2}}$$

The aforementioned functions clearly demonstrate that the relationships between tension and displacement, as well as between pretension and mass, are inherently nonlinear. This nonlinearity increases the system complexity, resulting in responses to sinusoidal wave forces that are fundamentally nonlinear. Consequently, the system does not demonstrate simple, linearly proportional behavior in response to wave inputs, leading to more intricate motion and tension behaviors, particularly under varying open-sea environments.

When the heaving motion is sufficiently small, the vertical displacement η_z can be approximated as zero. Under these conditions, Eq. (7) can be simplified to

$$T_x = \frac{EA}{l_0} \eta_x \quad (8)$$

$$T_z = 0$$

This simplification effectively reduces the system to a linear spring model in which the horizontal tension T_x is directly proportional to the horizontal displacement η_x , and no vertical tension is generated owing to the negligible heave motion.

The clump weight serves as an equivalent to tension, particularly in scenarios where installing a winch is impractical. The relationship between the mass of the clump and rope tension is shown in

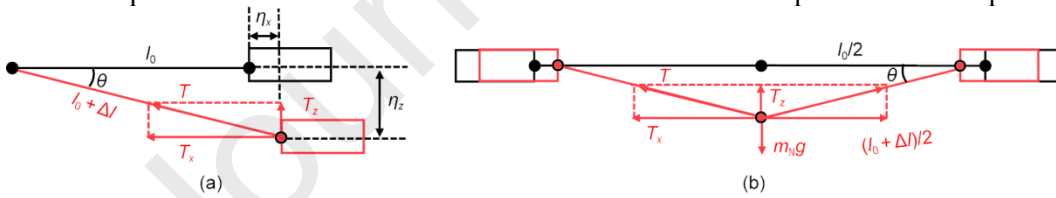


Fig. 5(b) and can be expressed as follows:

$$T_x = \frac{l_0}{\sqrt{l^2 - (l_0 + 2\eta_x)^2}} m_N g \quad (9)$$

$$T_z = \frac{m_N g}{2}$$

$$m_N g = 2EA(\tan \theta - \sin \theta)$$

In operational settings, both sides of the module are subjected to equal pretension from the ropes, maintaining the module in its initial position, such that $\eta_x = 0$. The elongated length l of the rope is governed by the relationship provided in Eq. (7). Notably, the elongation resulting from the pretension may be minimal, which can present challenges in maintaining the system within tolerance limits in real-world conditions.

4. Validations

4.1. Validation against diffraction and radiation theory

Although wave loads on a slender cylinder in long waves can be simulated with satisfactory fidelity using the Morison method in engineering applications, the importance of diffraction and radiation must be quantified. The potential flow theory is well established; when implemented with an appropriate numerical scheme, it offers a recognized approach that delivers rapid solutions with high precision for a wide range of hydrodynamic problems, provided strong nonlinearities are absent.

For a single box, the surge and heave response amplitude operators (RAOs) results are shown in Fig. 6(a) and (b), respectively, and are compared with the results obtained from potential flow theory incorporating an added linear spring. This comparison highlights the differences between the potential flow and Morison models at different wavelengths. Notably, the two models demonstrated significant discrepancies in short waves owing to their distinct radiation effects. As the wavelength increased, these discrepancies diminished because the modules tended to move in phase, and the influence of the radiation effects decreased. The results demonstrate that, despite the observed differences, the selected Morison model provides a reasonably accurate approximation of the potential flow model under long-WCs in this study.

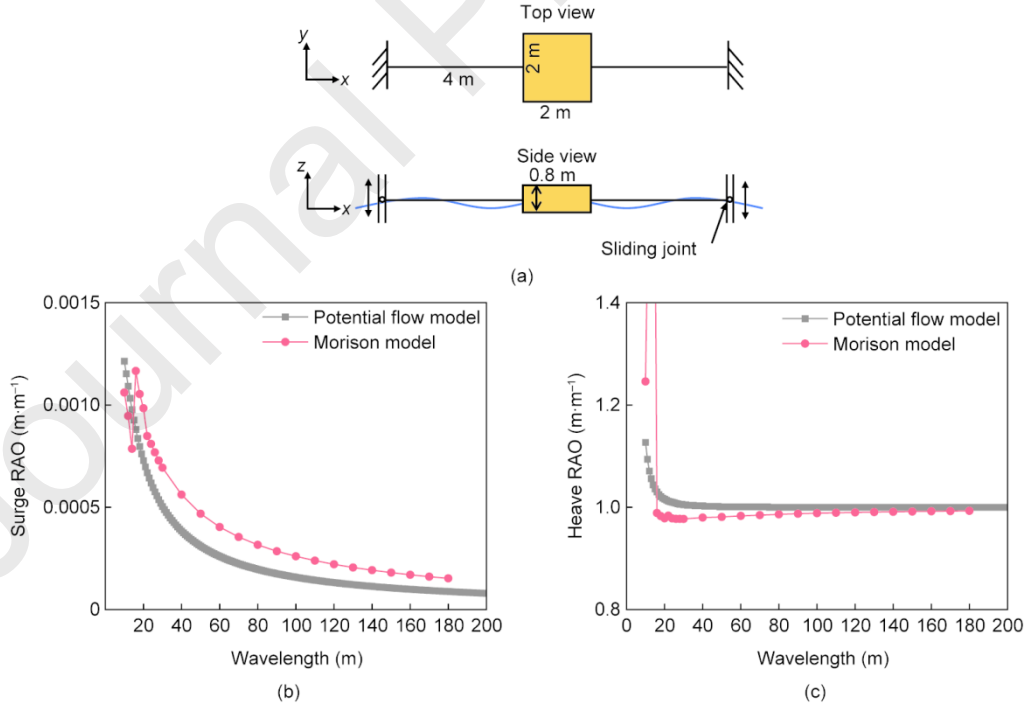


Fig. 6. (a) Single module model for validation. (b) Comparison of surge RAO and (c) comparison of heave RAO, calculated using the potential flow and Morison models.

The discrepancies observed in the long waves were primarily attributed to differences in the modeling of the spring and hydrodynamic parameters. In potential flow theory, a linear spring was utilized to represent restoring forces, whereas in the Morison method, a rope served a similar function in a one-dimensional context. A linear spring is defined to possess

the same axial stiffness as the rope. The results indicate that, when employing the Morison model, the heave RAO closely matched the outcomes from the potential flow model; the surge RAO values were relatively higher than those obtained from the potential flow model. This discrepancy arises because the rope tension comprises both horizontal and vertical components as the box oscillates with the waves, as shown in

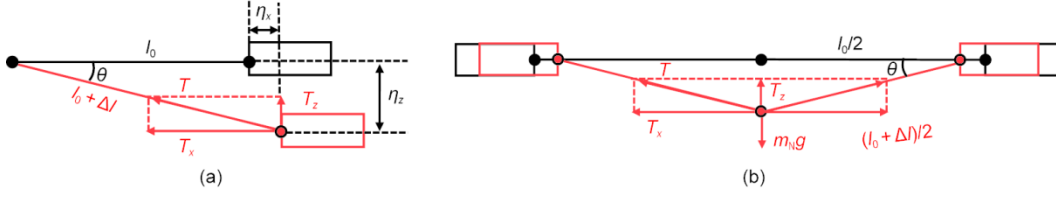


Fig. 5. Consequently, the restoring force exerted by the rope in the x and z directions became a nonlinear function of the surge motion. Additionally, in the Morison model, the selected hydrodynamic parameter C_A remained constant and remained invariant with wave frequency, unlike in the potential flow theory, where the added mass was frequency-dependent. Moreover, the inclusion of the drag coefficient C_D in the Morison model accounted for the viscous effects of water, neglecting the potential flow theory.

4.2. Validation against tank testing

Following the model test conditions outlined by Saitov and Jiang [31] and Jiang et al. [15,32], the full-scale model test parameters are listed in Table 3. The simulation model is shown in Fig. 7.

Table 3

Full-scale model test parameters.

Variable	Full-scale value
Floater length (m)	4.7
Floater width (m)	2.9
Floater height (m)	0.6
Floater material density ($\text{kg}\cdot\text{m}^{-3}$)	313 (dry), 352 (wet)
Free-floating draft (m)	0.19 (dry), 0.21 (wet)
Array length (m)	10.4
Array width (m)	10.7
Gap between floaters (m)	1.0
Rope material density ($\text{kg}\cdot\text{m}^{-1}$)	1.8

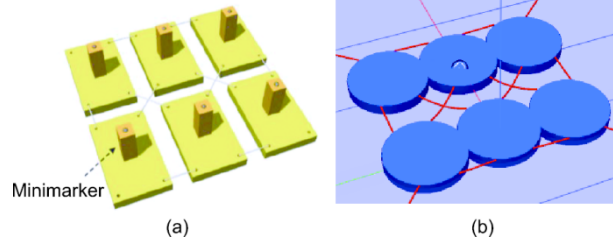
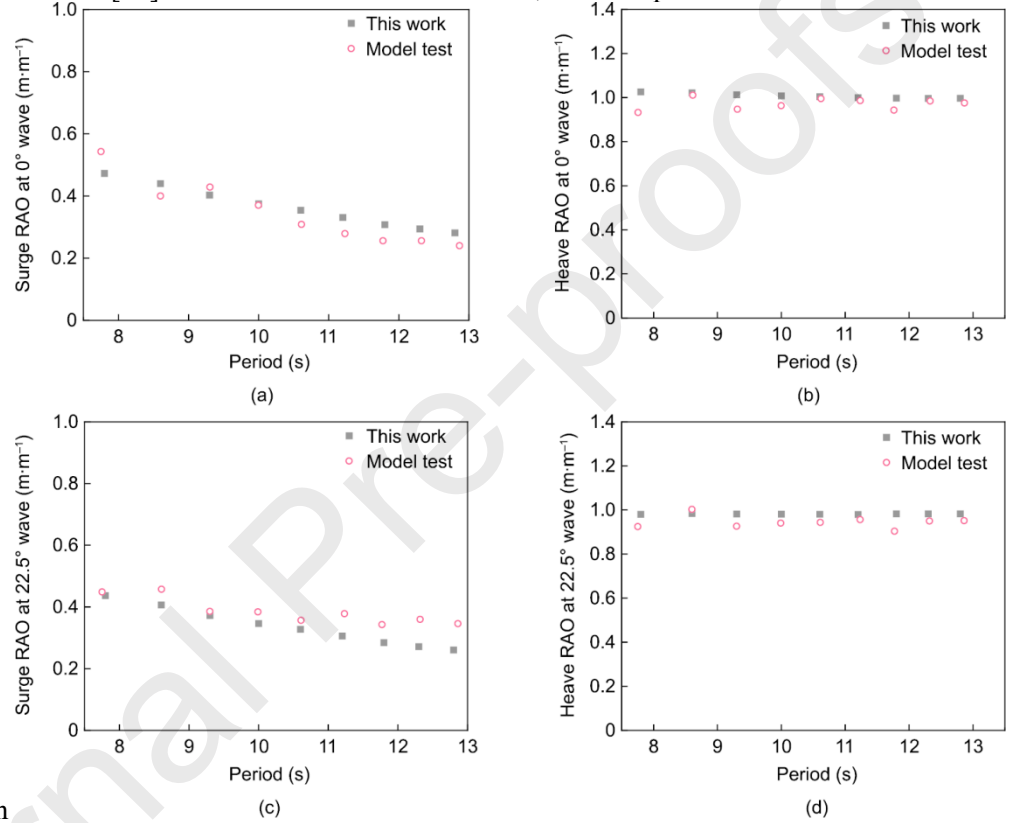


Fig. 7. FPV array model in tank testing and numerical simulation.

Numerical simulations were conducted using the RIFLEX software, and the simulation results were compared with the experimental data reported in Ref. [15]. The WCs are listed in Table 4, and comparisons between the simulation and



model test results are shown in

Fig. 8. The floaters were modeled as rigid bodies when using the Morison model. In the right panel of Fig. 7, the boxes are represented as cylinders because the Morison model is applied exclusively to line elements, resulting in cylindrical representations within the visual geometry of the software. However, the hydrodynamic parameters, specifically C_D and added mass coefficient C_A , were selected based on rectangular cross-sections, in alignment with the actual physical configuration. For a rectangular cross-section with an L/D of 2:1, C_D was set as 1.6, and C_A was set as 1.7, according to Appendices D and E in DNVGL-RP-C205 [30].

Table 4

Validation conditions (VCs).

VC	Parameter
	Wave amplitude (m) Wave period (s)

VC1	0.95	7.8
VC2	1.05	8.6
VC3	1.35	9.3
VC4	1.55	10
VC5	1.70	10.6
VC6	1.95	11.2
VC7	2.25	11.8
VC8	2.40	12.3
VC9	2.55	12.8

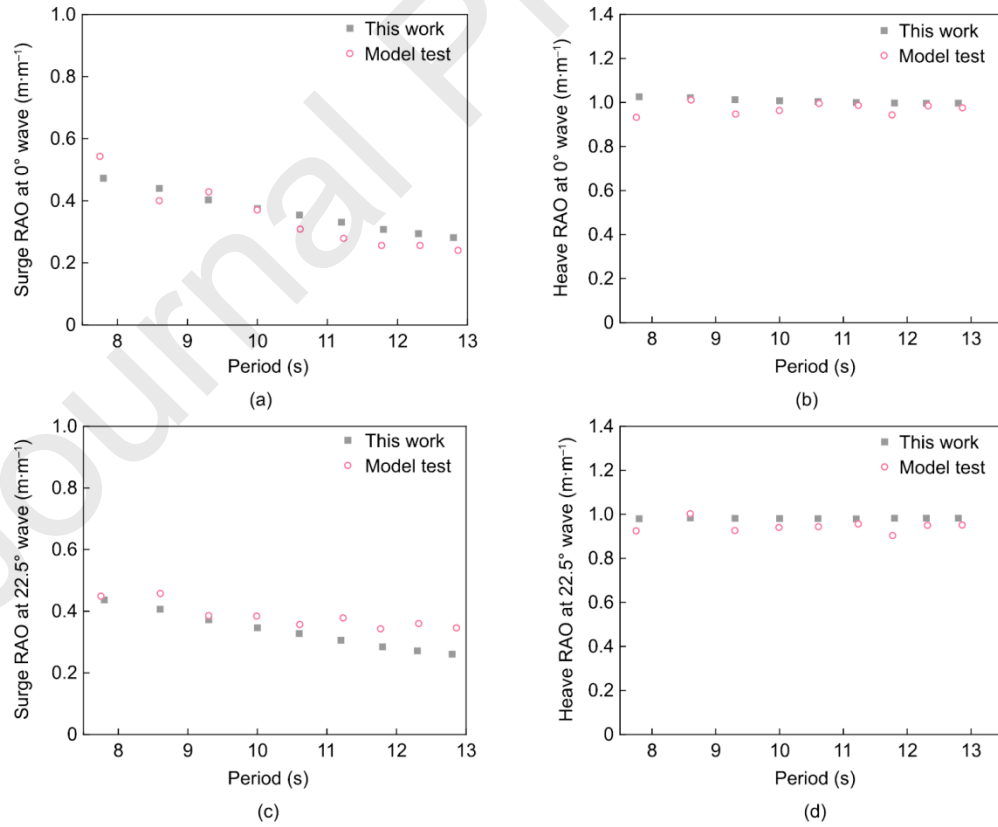
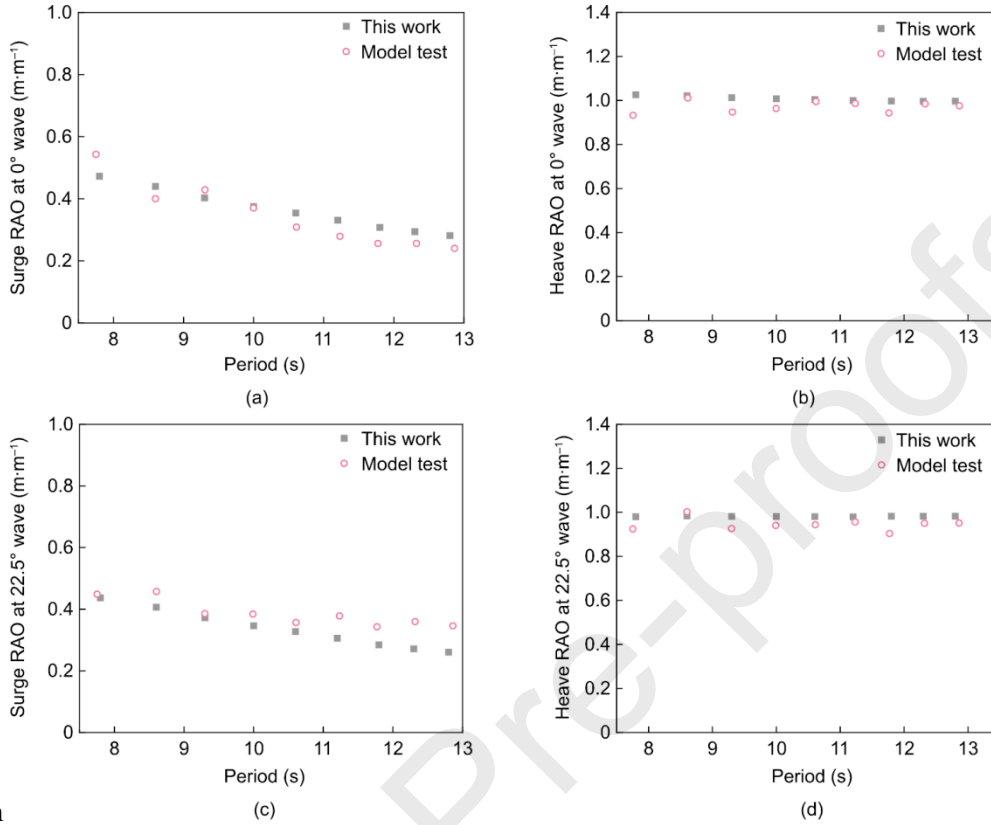


Fig. 8. Validation results between tank test and numerical simulation. (a) Surge RAO at 0° wave, (b) heave RAO at 0° wave, (c) surge RAO at 22.5° wave, (d) heave RAO at 22.5° wave.

In this study, hydrodynamic damping was modeled using linear drag terms from Morison's equation with no quadratic or viscous damping. Although some damping was included for numerical stability, it was not calibrated against the experimental data. This simplification may underestimate the damping, and addressing this limitation is recommended for future work.



As shown in

Fig. 8, for wave incident angles of 0° and 22.5° , the surge and heave motion RAOs are similar. The surge motion RAOs gradually decreased with increasing wave period, whereas the heave motion RAOs remained at approximately $1.0 \text{ m} \cdot \text{m}^{-1}$, indicating insensitivity to changes in the wave period. Although minor discrepancies exist between the numerical simulation results and model test data—attributable to various uncertainties in the experimental setup—these differences were within an acceptable range. Therefore, the reliability of the numerical method employed in this study is substantiated.

5. Results and discussion

In this section, the Morison model is employed without accounting for hydrodynamic interactions between modules. Multi-body interactions were limited to the mechanical connections among modules. For the solar-web model, a square cross-section was assumed, with the added mass and drag coefficients set to $C_D = 2.20$ and $C_A = 1.51$, respectively. Owing to the symmetry of the array, all results demonstrated symmetrical behavior, permitting analysis of only one half of the structure when the incident wave direction was 0° . This discussion began with a single-line array, primarily constrained to two-dimensional motion, and subsequently extended to a three-dimensional array configuration.

5.1. Parameter influence on single array

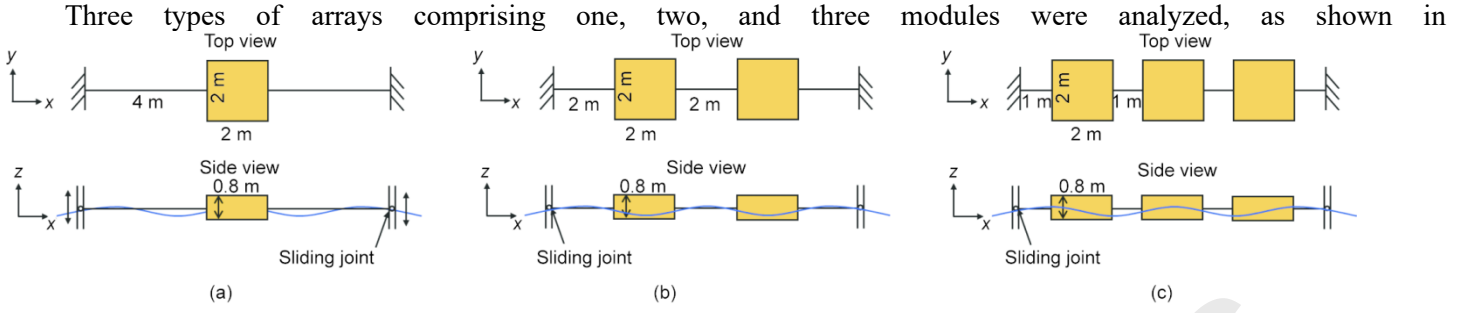


Fig. 9. The total length of each array was 10.0 m, and the distance between the modules was uniform and equal to the length of the connecting ropes.

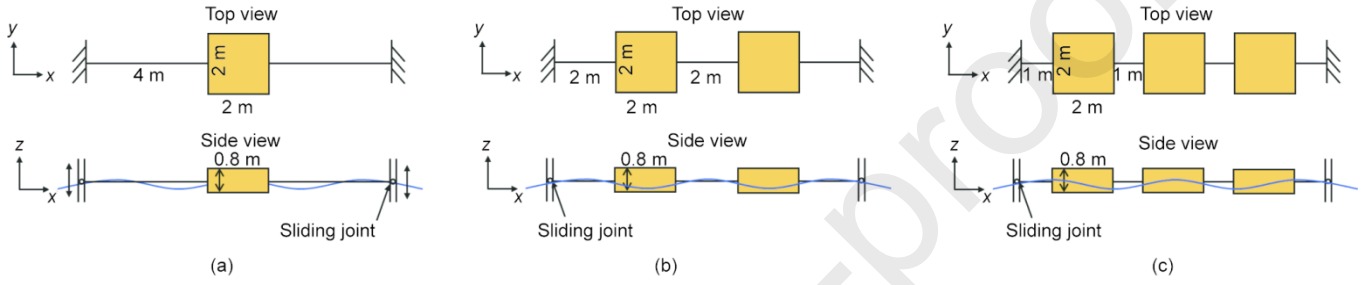


Fig. 9. Configuration of three types of arrays composed of (a) 1×1 , (b) 2×1 , and (c) 3×1 modules.

5.1.1. Wavelength and steepness

The WCs are listed in Table 5, with a constant wave steepness of 0.02 across all scenarios.

Table 5

Wave conditions.

WC	Parameter	
	Wave amplitude (m)	Wavelength (m)
WC1	0.5	50
WC2	0.6	60
WC3	0.7	70
WC4	0.8	80
WC5	0.9	90
WC6	1.0	100

WC7	1.1	110
WC8	1.2	120
WC9	1.3	130
WC10	1.4	140
WC11	1.5	150
WC12	1.6	160
WC13	1.7	170
WC14	1.8	180
WC15	1.9	190
WC16	2.0	200

The ends of the ropes were fixed to the tightly tensioned radial lines. Under practical WCs, these radial lines may experience vertical motion, whereas horizontal displacements remain negligible. To more accurately represent this behavior, vertical DoFs were released at both ends of the array, allowing for heave motion while preserving horizontal stability. This configuration enables the structure to accommodate wave-induced vertical displacements without compromising its overall integrity. No pre-tension was applied to the system in this analysis.

The

RAOs

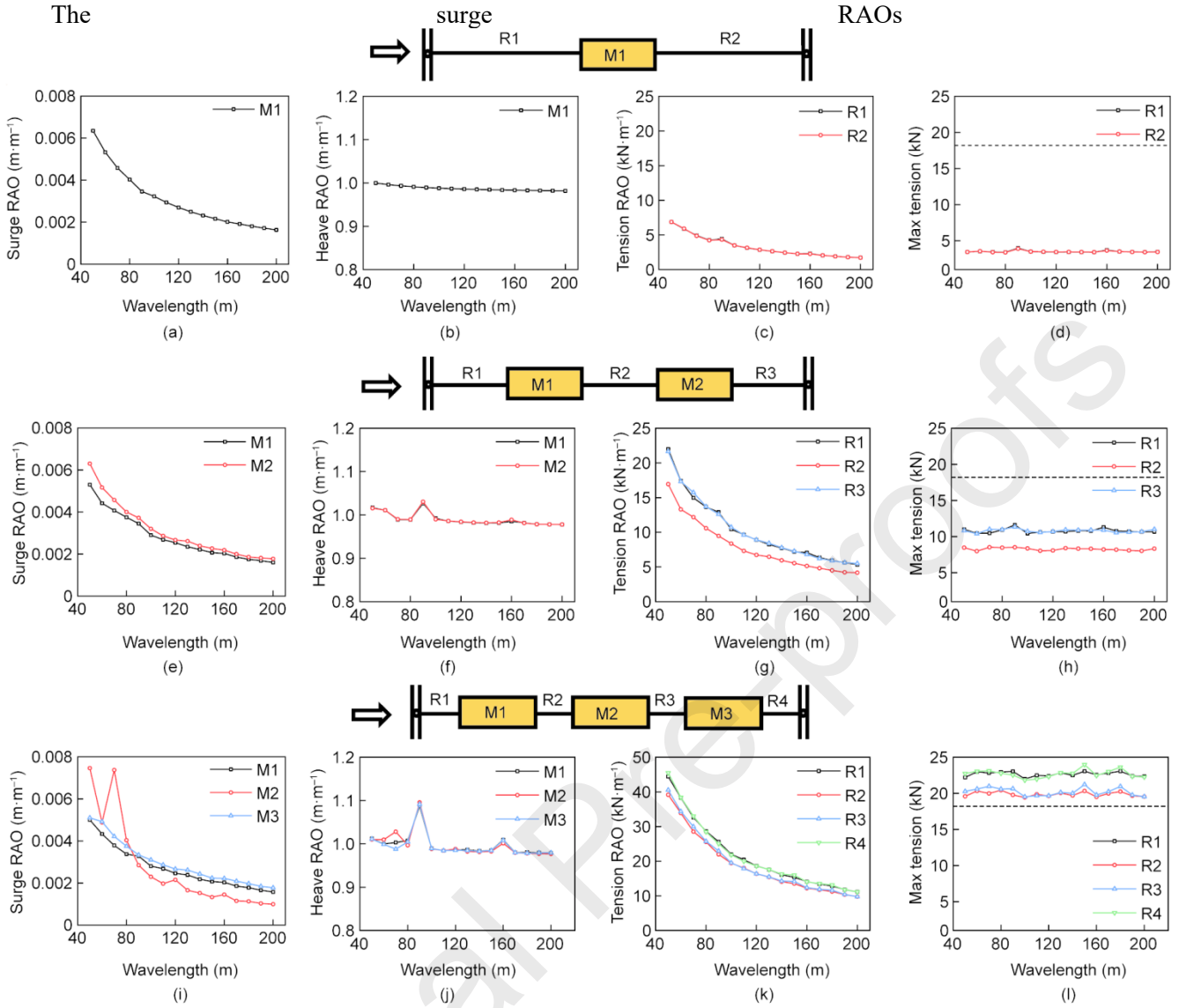


Fig. 10(a), (e), and (i) decrease as the wavelength increases, whereas the heave RAO varies slightly near $1.0 \text{ m} \cdot \text{m}^{-1}$ in

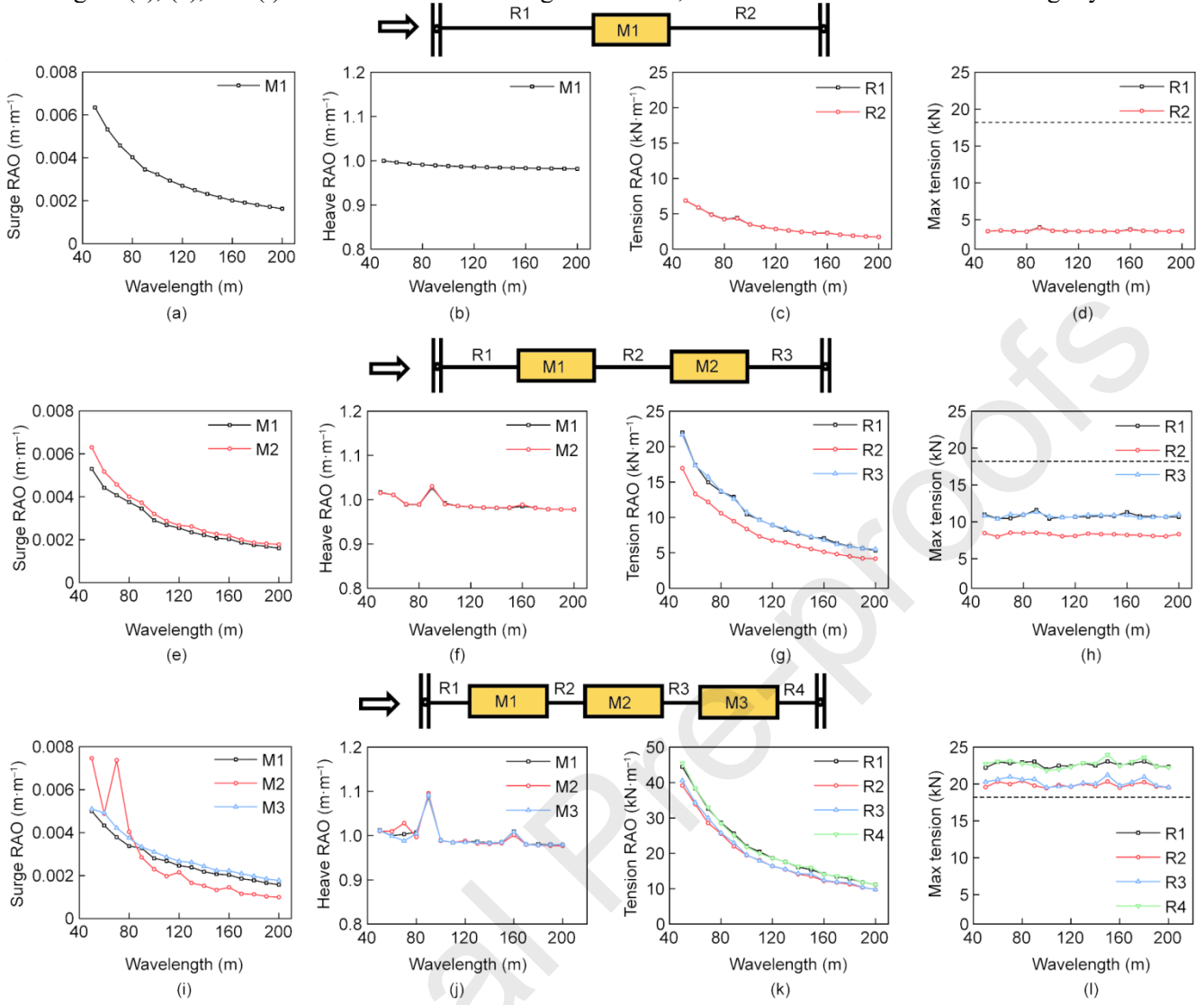


Fig. 10(b), (f), and (j). This behavior was attributed to the selected wavelengths being significantly larger than the characteristic system length, causing the heave RAO to gradually stabilize around unity. The overall surge motion was minimal owing to the restraining effect of the ropes, with surge amplitudes in all cases remaining below 1% of the gap between modules. Therefore, when appropriate pretension is applied to the ropes, the risk of collision between floaters is effectively eliminated.

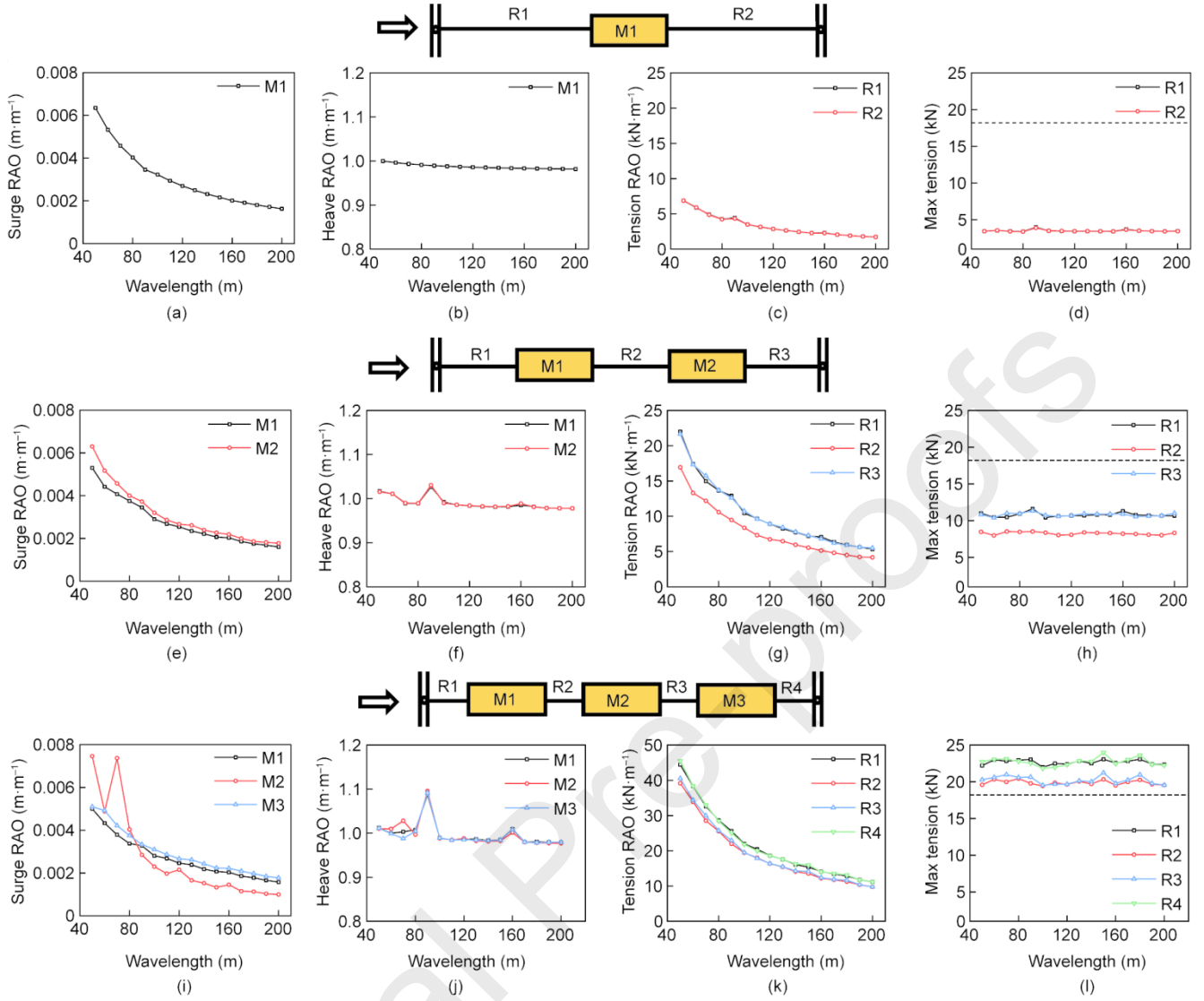


Fig. 10. RAO and tension variations with wavelength (wave steepness = 0.02). R1–R4 denote Ropes 1–4 and M1–M4 denote Modules 1–4. (a–c) Surge and heave RAOs for M1, and tension RAO for R1 and R2. (d) Maximum tensions of R1 and R2. (e–g) Surge and heave RAOs for M1 and M2, and tension RAO for R1–R3. (h) Maximum tensions of R1–R3. (i–k) Surge and heave RAOs for M1–M3, and tension RAO for R1–R4. (l) Maximum tensions of R1–R4.

However, rope tension is a critical concern. As shown in

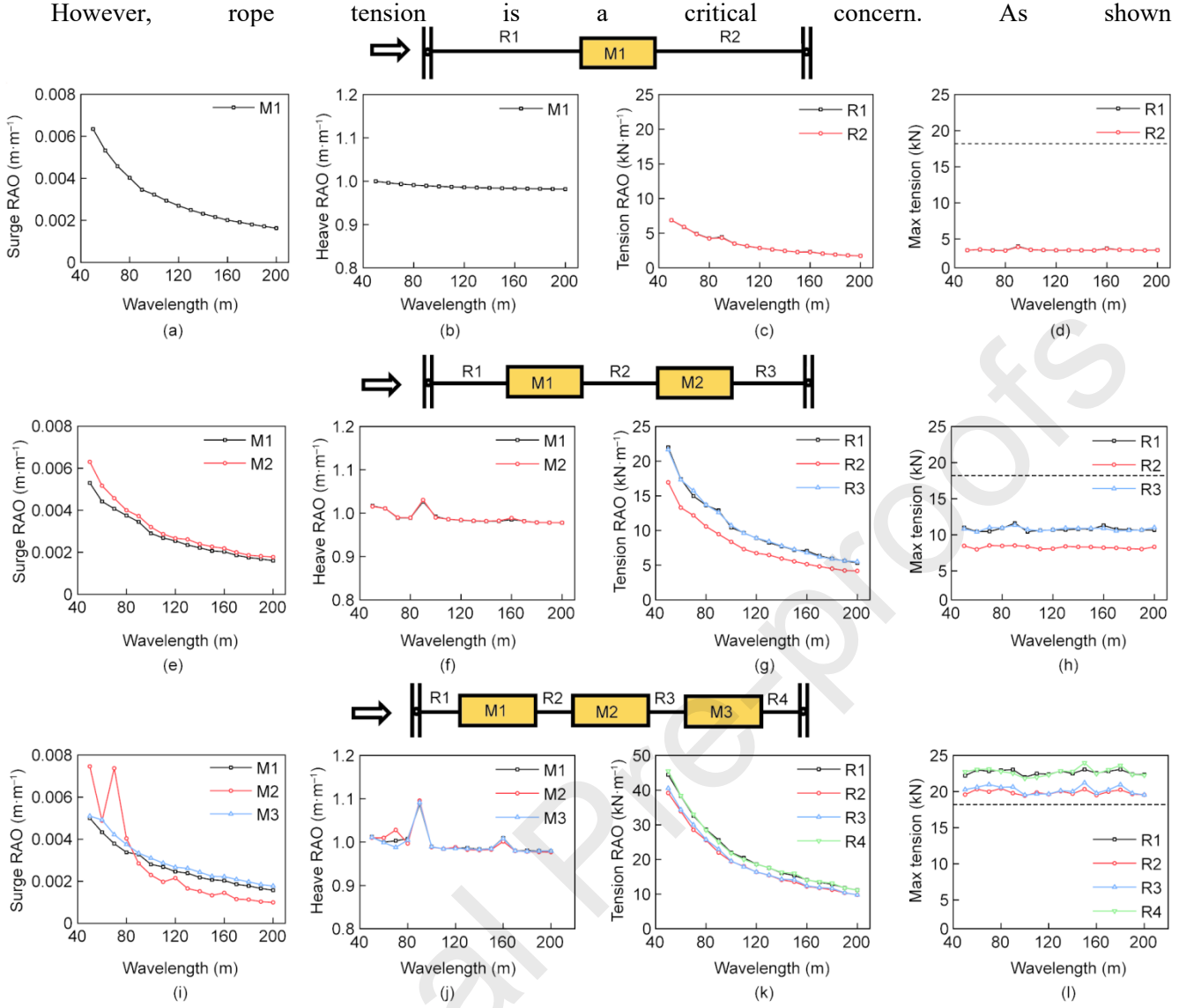


Fig. 10(c), (g), and (k), the tension RAOs increase with decreasing rope length. The outer ropes experience higher tensions than the inner ones, indicating that dynamic loads are partially relieved within the rope-connected structure. This

behavior is consistent across all array configurations. Furthermore, as shown in

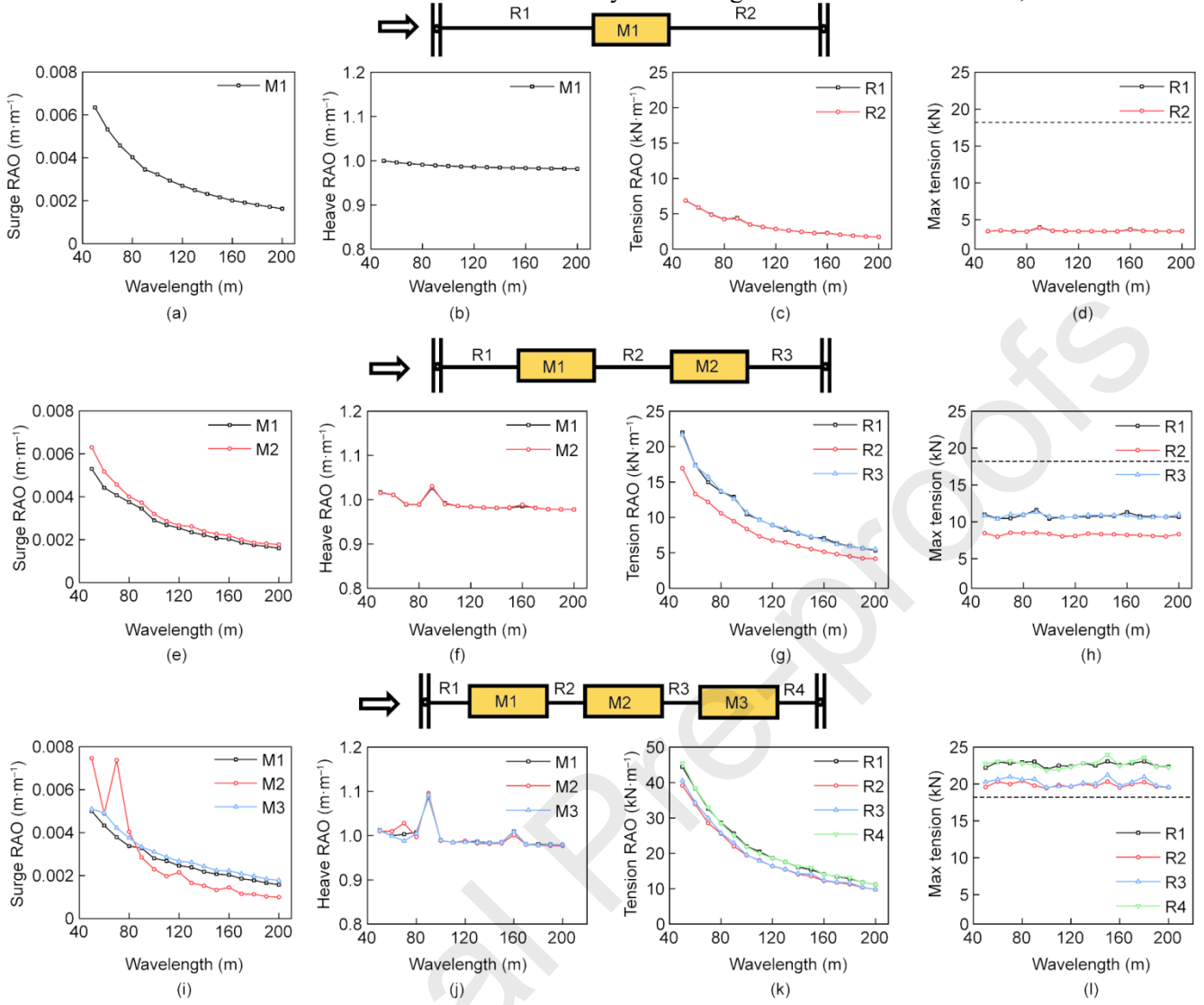


Fig. 10(d), (h), and (l), the maximum tension in the ropes increases substantially as the rope length decreases across all array configurations. According to

Variables	Full-scale value
Length (m)	2.0
Width (m)	2.0
Height (m)	0.8
Material density ($\text{kg} \cdot \text{m}^{-3}$)	512
Draft (m)	0.4

Gap between modules (m) 1.0

Total length/width of array (m) 10.0

The connection lines utilized in this study were composed of polyester ropes. As a synthetic material, polyester is immune to corrosion and demonstrates superior tension fatigue, out-of-plane loading, and torsion compared with steel components. Owing to its low density, polyester rope necessitates lighter connecting hardware and reduced structural bracing, thereby enabling a more efficient system design. The specific parameters of the polyester ropes utilized in this study are listed in **Error! Not a valid bookmark self-reference..**

Table 2, the safe working load for the selected 38 mm rope is 18.2 kN. Notably, in the 3×1 array with 1.0 m ropes, the maximum tension surpassed this safe limit, indicating a potential risk of rope failure.

This variation in tension was directly related to rope length, which determined the axial stiffness $k = EA/l_0$. As the rope length decreased, its stiffness increased, resulting in higher tension under identical loading conditions. Consequently, shorter ropes were subjected to greater forces, which could exceed the safe working limits, as shown in

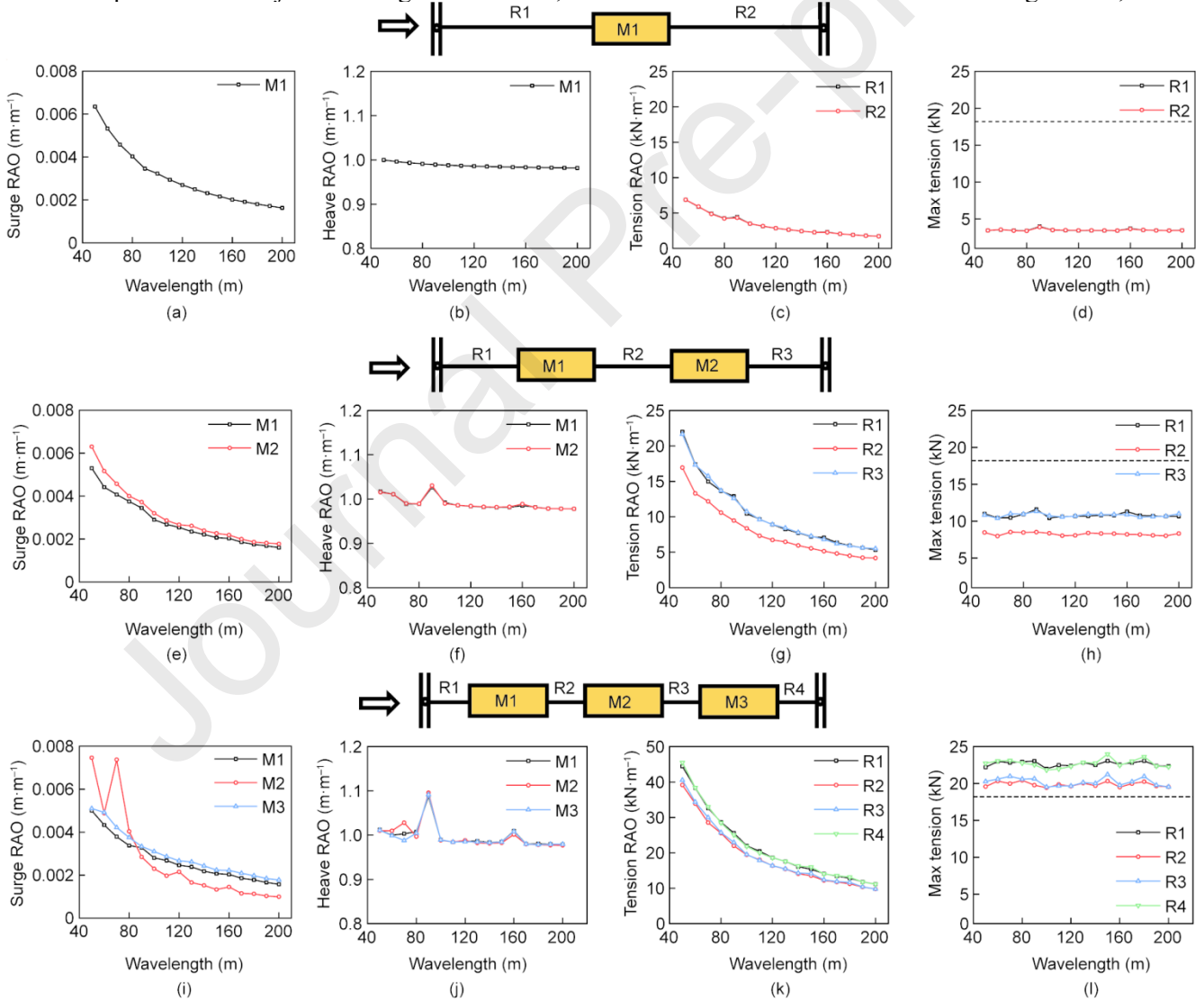


Fig. 10(l) for the 3×1 module.

The results further indicate that the tension in the “interior” ropes was generally lower than that in the ropes connected to the boundaries. This can be attributed to the phase differences in the surge motions of the modules; the elongation of the interior ropes was determined by the relative position of the two connected modules, leading to lower tension compared with that of the ropes at the boundaries.

The motion and tension responses were highly dependent on the nature of the boundary conditions—specifically, whether the boundary points were fixed or free to move vertically. These boundary conditions have a direct impact on the vertical components of rope tension. As shown in Fig. 11, fixed boundaries result in a substantial increase in system tension, often exceeding safe load limits and imposing excessive stress on the ropes. Introducing more flexible boundary conditions, such as reducing pretension in the frame lines, can effectively mitigate these risks.

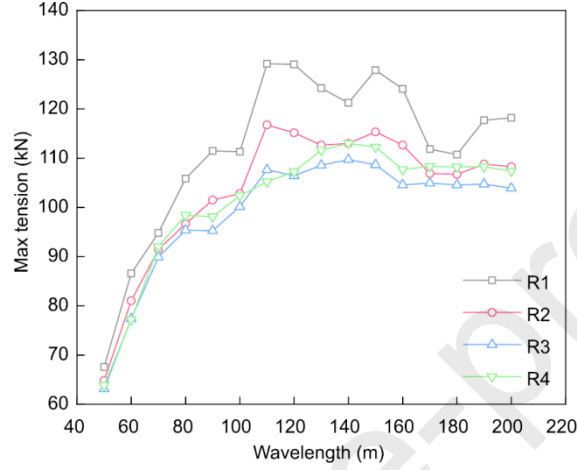


Fig. 11. Rope tensions in the 3×1 array with fixed boundaries.

The tension response under free boundary conditions demonstrated a distinct variation tendency from that under fixed boundary conditions. With free boundaries, the system demonstrated reduced sensitivity to changes in wave height,

resulting in relatively constant tension values, as shown in

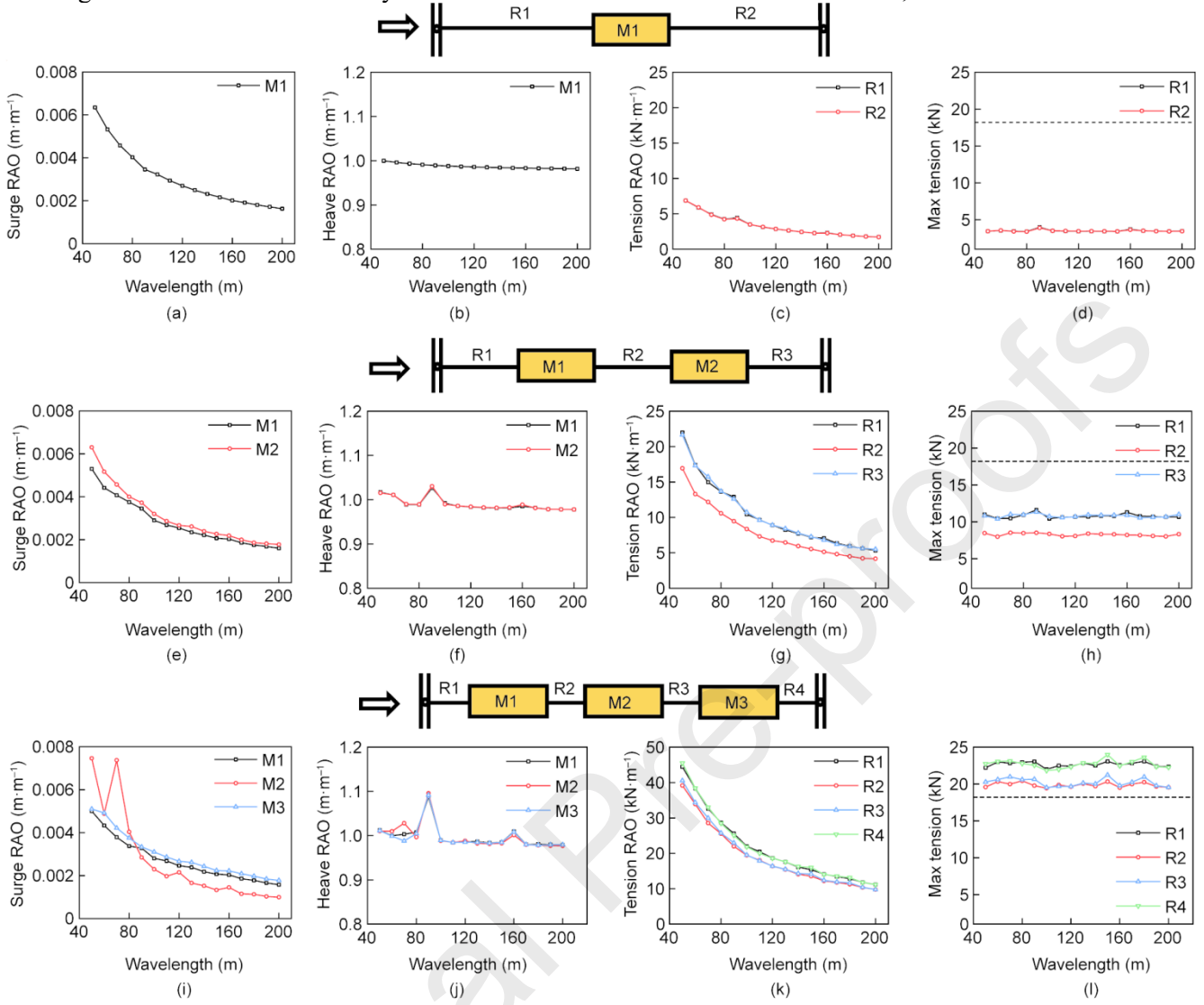


Fig. 10(l). Conversely, with fixed boundary conditions, an increase in wave height directly corresponds to higher absolute tension values.

The Morison model was unsuitable for comparing the motion of the system in short waves. Instead, the potential flow theory provides a more accurate representation of both the peak response and variation trends of surge and heave motions

for wavelengths shorter than five times the characteristic dimensions of the system, as shown in

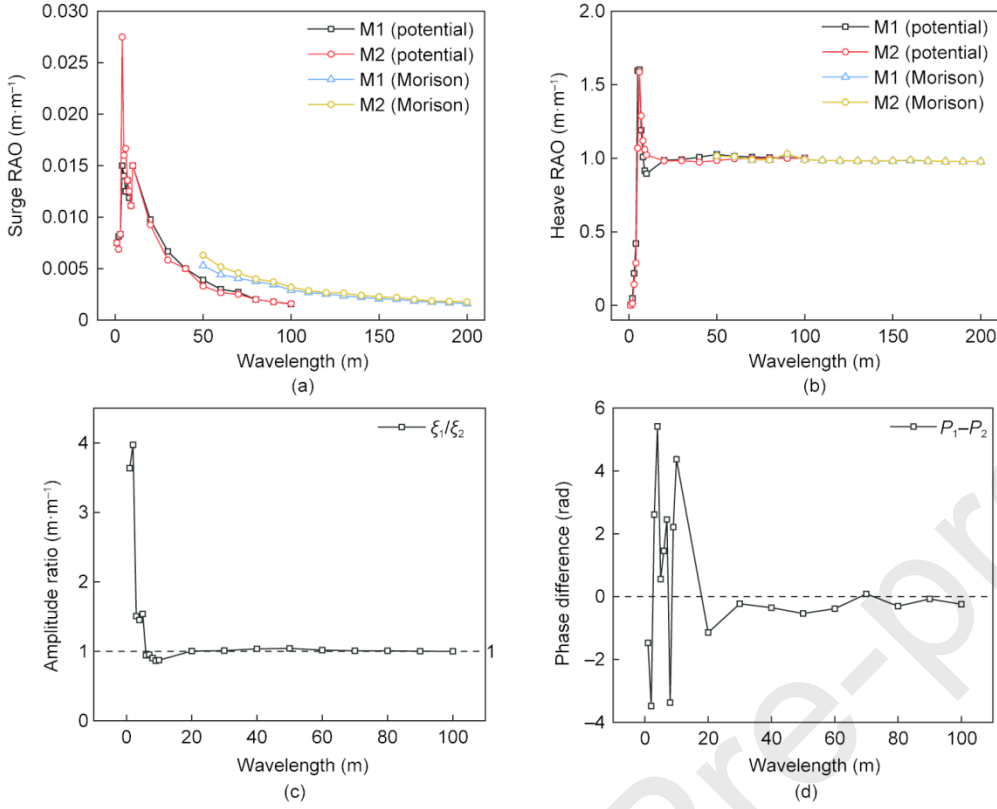
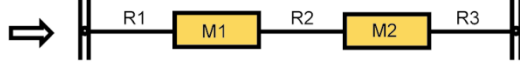


Fig. 12(a) and (b). For short waves, the transmittance was significantly lower than that for long waves. As shown in

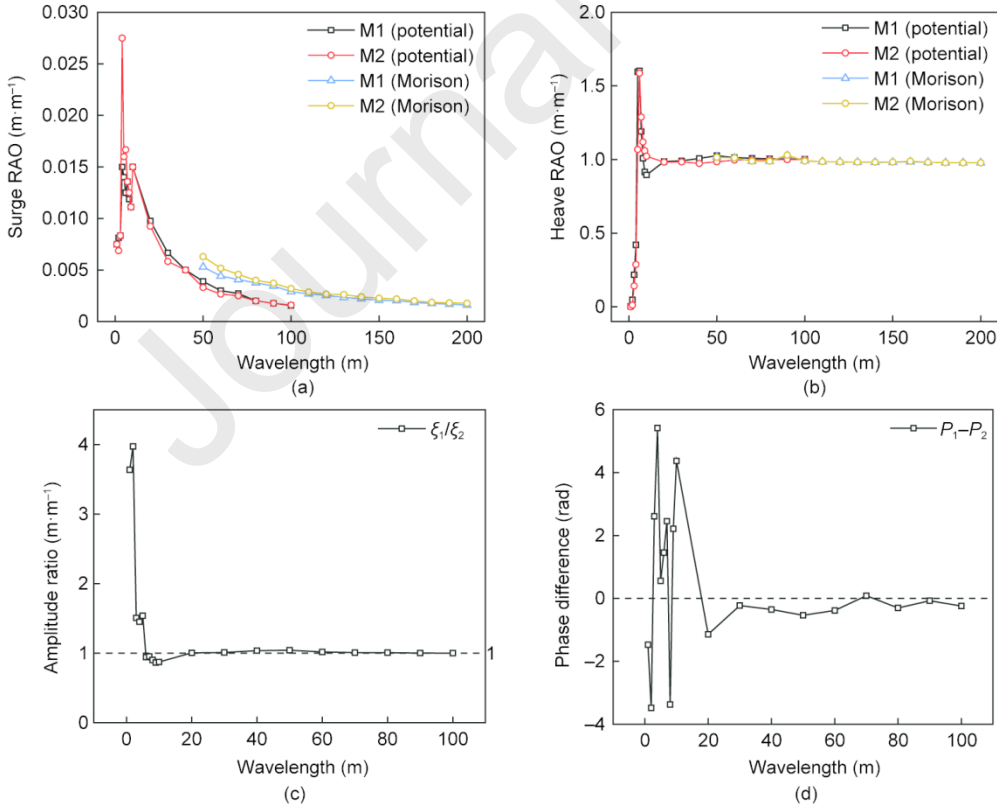


Fig. 12(c), the amplitude ratio between Modules 1 and 2 was high for wavelengths shorter than 5 m, and gradually decreased to 1 as the wavelength increased. Accordingly, in

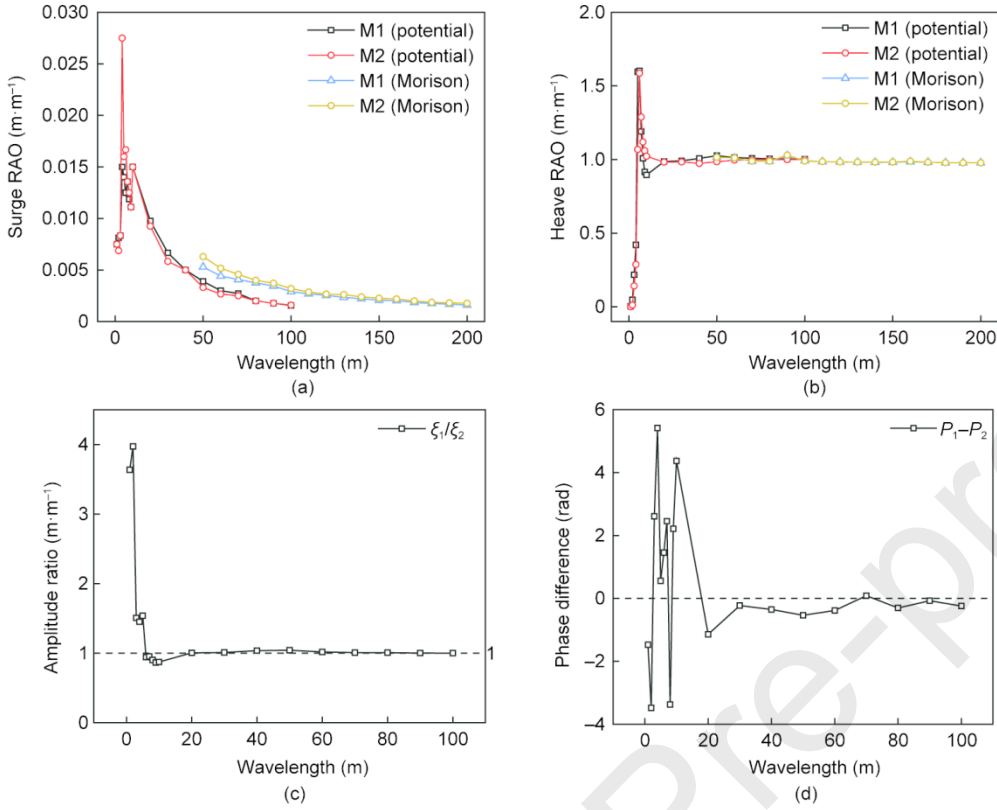
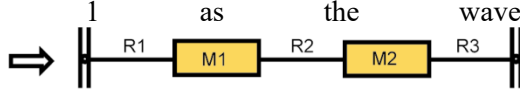


Fig. 12(d), the phase difference converges toward zero at longer wavelengths. For this flat, surface-covering structure, the surge amplitude remains minimal across all wavelengths, rendering the risk of collision negligible and hence insignificant to our analysis. However, the heave motion demonstrated a pronounced phase difference under short-WCs, which significantly influenced rope tension.

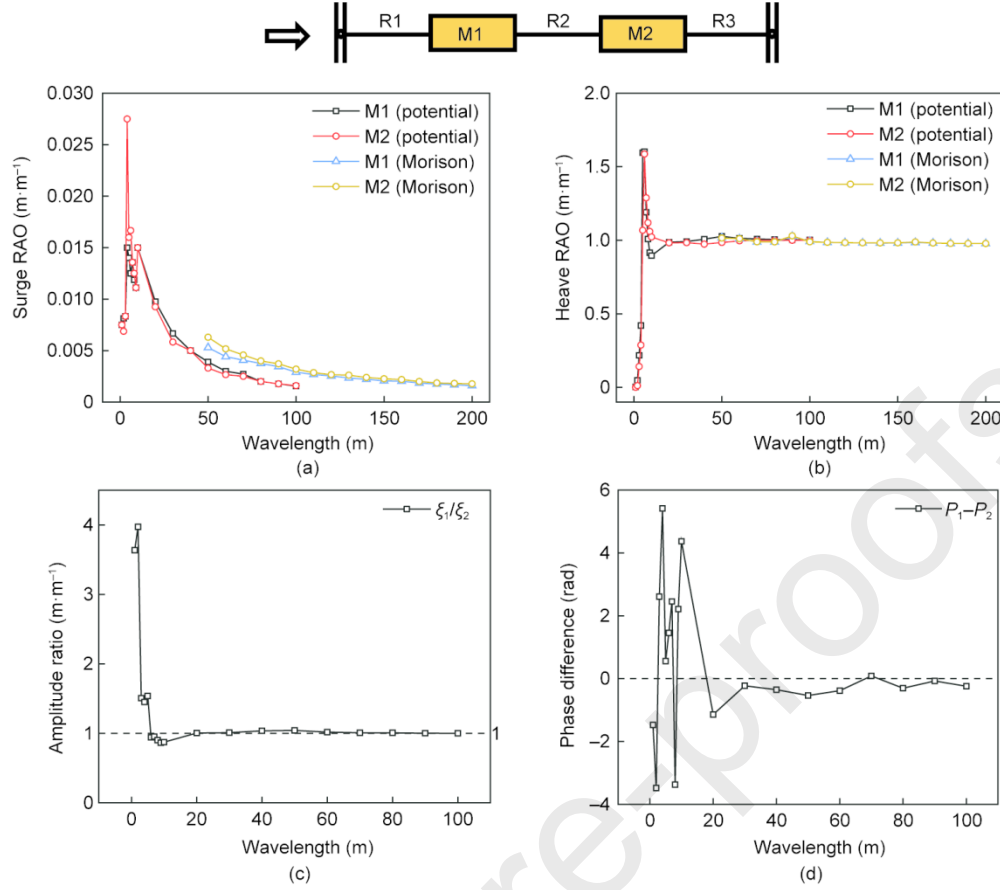


Fig. 12. System response: (a) surge and (b) heave motion in short waves; (c) amplitude ration and (d) phase difference between modules across wavelengths. ζ_1 and ζ_2 denote the heave motion amplitudes of M1 and M2, respectively; P_1 and P_2 denote their corresponding phases, respectively.

5.1.2. Gaps

As established in the previous section, the gap—defined by the rope length—significantly influenced the tension distribution within the system. By adjusting the gap, the tension can be redistributed across the ropes. As the boundary ropes were subjected to higher tension than the interior ropes, increasing the length of the boundary ropes while reducing the length of the interior ropes could help achieve a more balanced load distribution. At an optimal gap, the tension in all the ropes can be maintained below the safe working load.

Assuming the case of a 100 m long wave and a three-module array as an example, the comparison of different gaps is shown in Fig. 13. The relationship between the maximum tension and gap between modules is not inversely proportional, as previously anticipated. For Ropes 1 and 4, an increase in the length resulted in a decrease in the tension. However, for ropes 2 and 3, the relationship between the maximum tension and gap did not follow this inverse proportionality, as initially deduced. This deviation may be attributed to the reduction in wave phase difference between modules as they were positioned closer together. When the modules were positioned closer to each other, their relative motion phases became more synchronized, resulting in reduced relative displacements between them. This synchronization in motion led to less strain on the connecting ropes, thereby lowering the maximum tension.

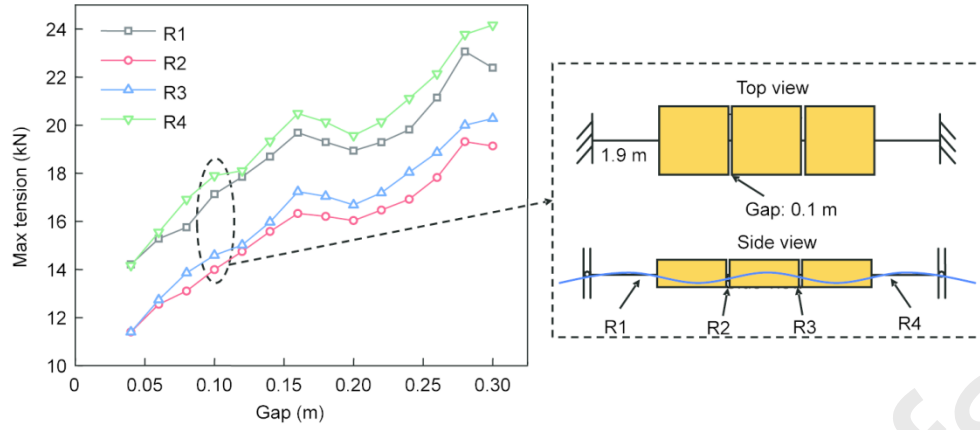


Fig. 13. Relationship between the maximum tension and gap between modules.

5.1.3. Pretension

In scenarios where no pretension was applied to the system, large amplitude variations were observed in the tension, commonly referred to as “snap load” in a rope system. This effect can negatively impact rope durability. To address this, an optimized pretension level must be applied to ensure that the ropes remain taut, thereby preventing the snap effect that occurs when the ropes become slack and suddenly tighten. Clump weights were implemented as a pretension strategy in the dynamic model and contributed to the initial static equilibrium of the moored system.

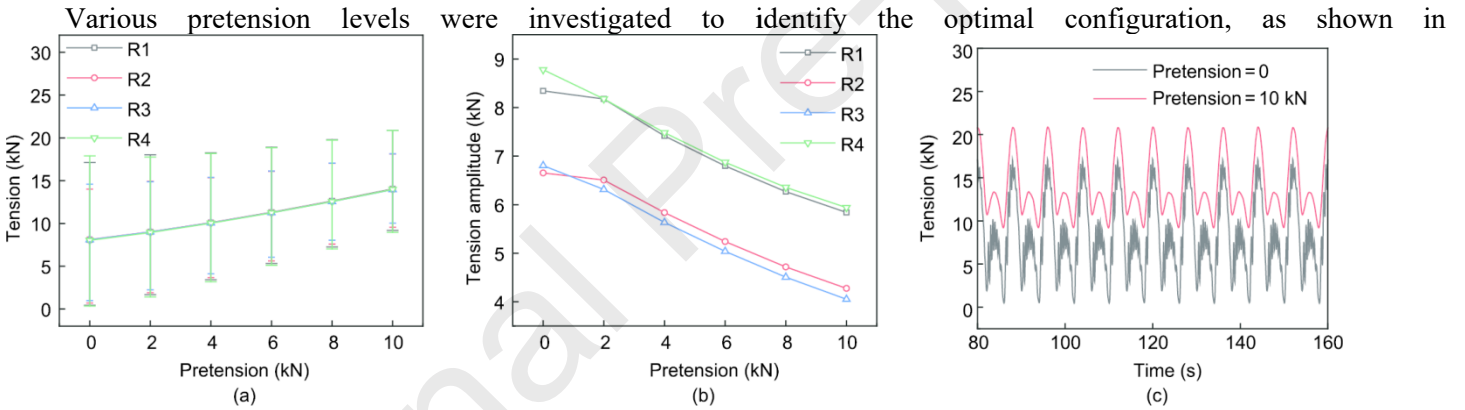


Fig. 14(a) and (b). The error bars represent the variation between the maximum and minimum tension values. Based on the results, increasing pretension reduced the amplitude of tension variation. Although the absolute tension in the ropes increased with higher pretension, the decrease in dynamic tension variation outweighed this increase, thereby demonstrating the effectiveness of pretension in mitigating dynamic loads, as shown in Fig. 14(c).

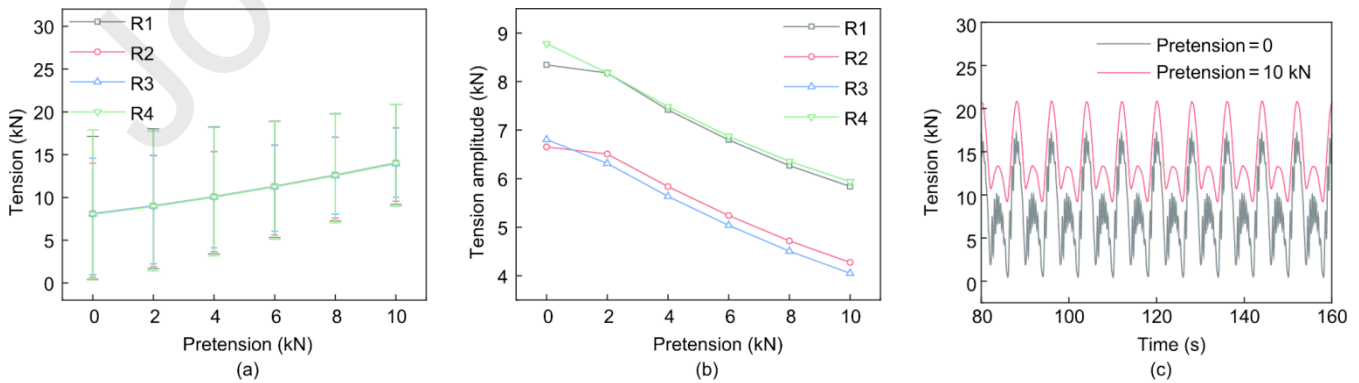


Fig. 14. Variation in (a) maximum, minimum, and mean tension values and (b) tension amplitude, with variations in pretension; and (c) time-domain tension response with and without pretension.

5.2. Modularized large array results

All the modules shared similar properties to those in the previous single-array configurations. The outer boundaries were equipped with sliding joints, permitting only vertical movement, whereas the inner connections were ropes with no applied pretension.

5.2.1. Wave direction

As shown in Fig. 15, the direction of wave incidence significantly influenced the distribution of rope tensions within the system. Tension patterns were symmetrical along the x -axis. When the wave direction was 0° , the highest tensions were observed in ropes aligned parallel to the direction of wave propagation. As the wave angle increased and a larger component of the wave force acted along the y -axis, the tensions in the vertical ropes correspondingly increased. At a wave direction of 45° , the tension distribution became symmetric along the diagonal of the array, resulting in a more uniform load across all ropes, each operating within safe load limits. Consequently, careful consideration of wave angle during array installation is essential to prevent overloading of the ropes.

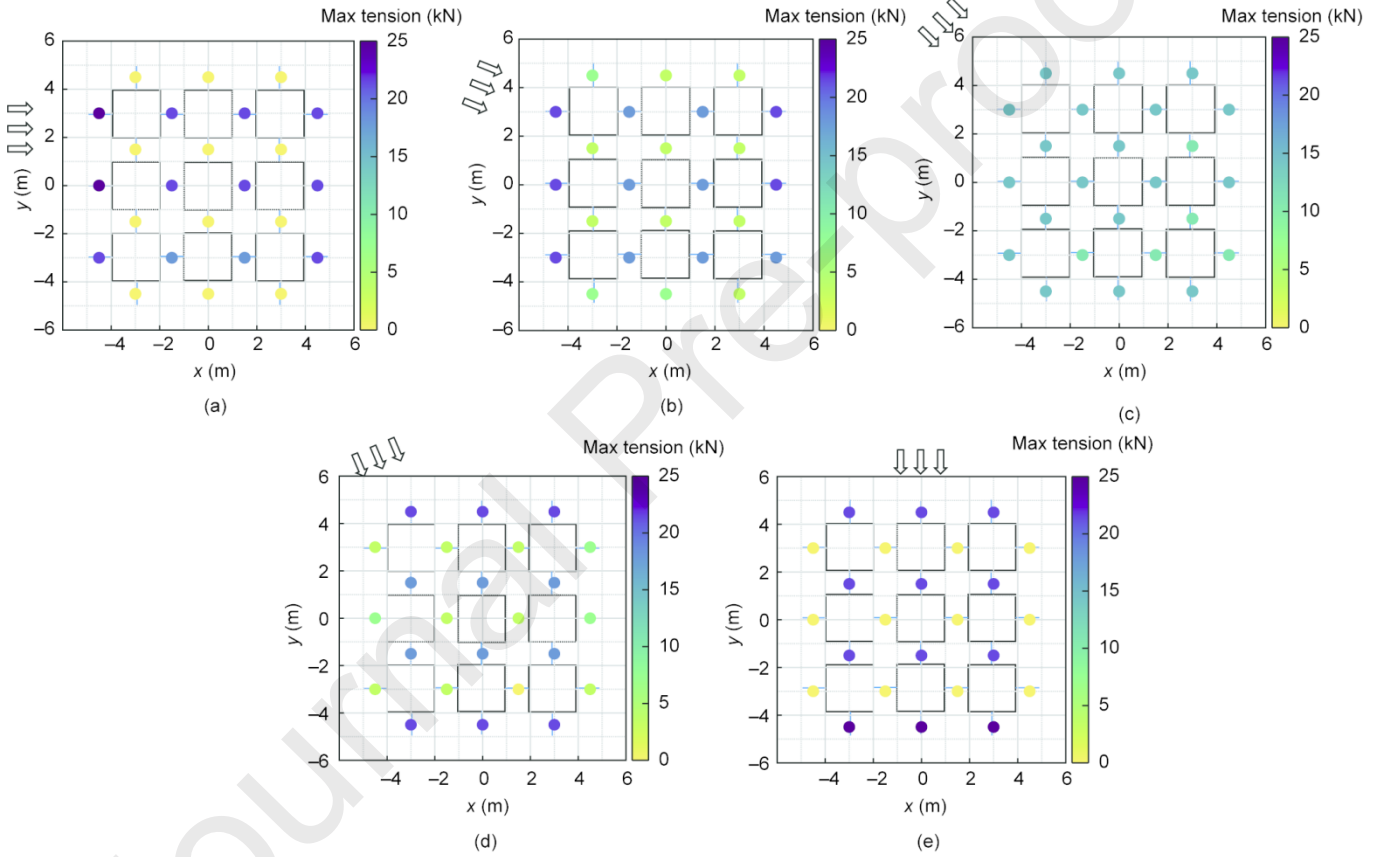


Fig. 15. Distribution of maximum rope tensions in a 3×3 array under a wavelength of 100 m at wave directions of (a) 0° , (b) 22.5° , (c) 45° , (d) 67.5° , and (e) 90° .

5.2.2. Rope failure analysis

A comparison between

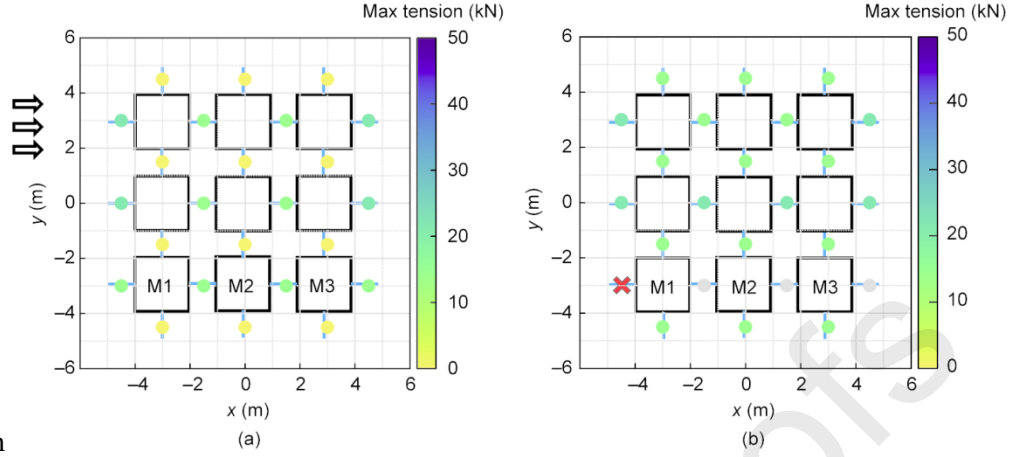


Fig. 16(a) and (b) demonstrates that the failure of a horizontal rope results in a significant increase in the tension within the vertical ropes. Without the restriction of the horizontal rope, the surge motion of the modules connected by the broken rope increased significantly, transferring additional tension to the remaining vertical ropes. This increased surge motion can also cause snap loads in ropes aligned horizontally with the failed section. The gray points in

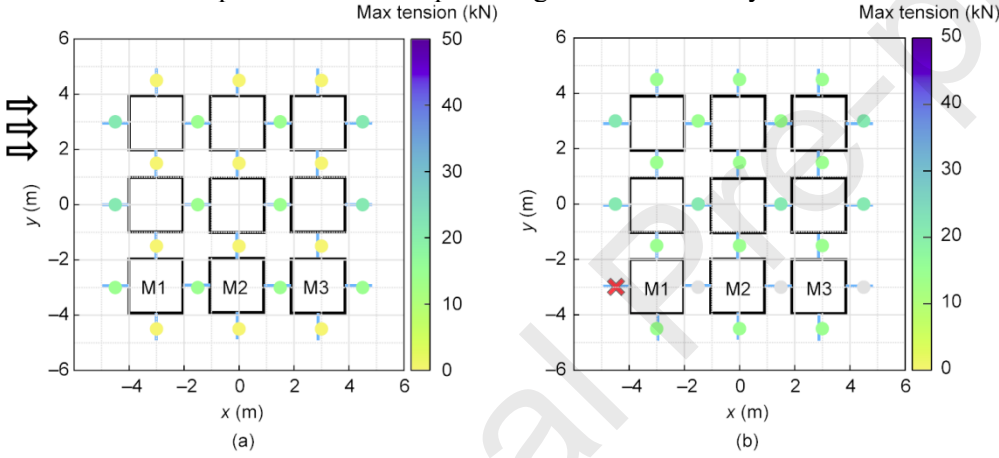


Fig. 16(b) indicate the subsequent failure of other ropes, resulting from redistributed loads and increased motion. The results indicated that in the event of a break, the load could be redistributed to the ropes perpendicular to the wave direction. However, the current design must be optimized to ensure that the remaining load on the ropes that are aligned with the wave direction remained within their safe working limits.

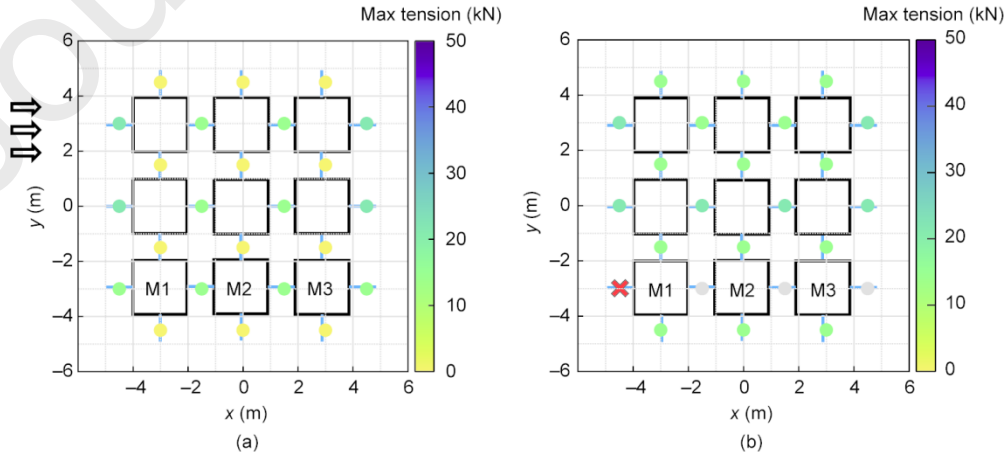


Fig. 16. Distribution of maximum rope tensions in a 3×3 array (a) before and (b) after the failure of the selected rope.

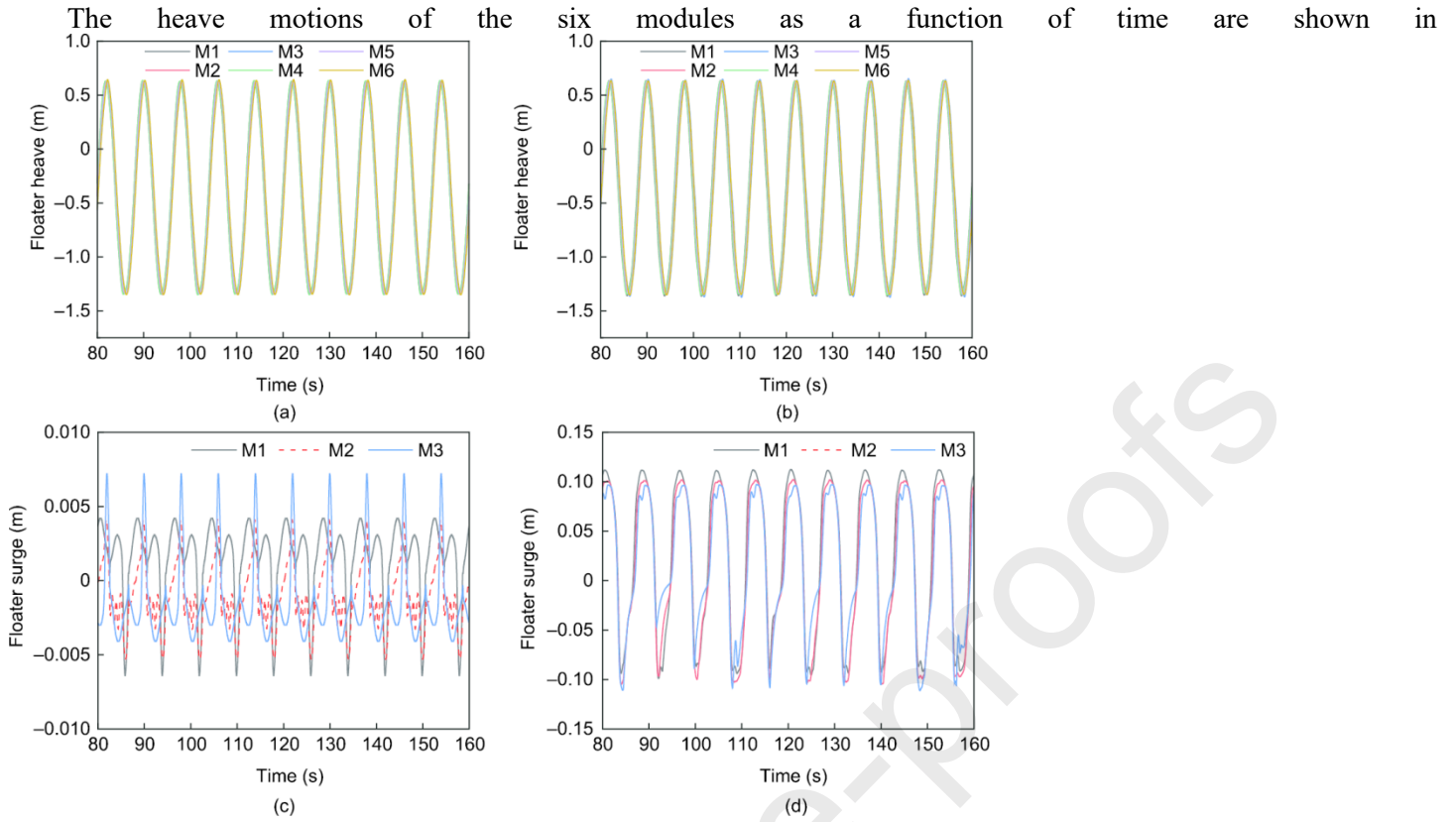


Fig. 17(a) and (b). All modules followed a consistent motion pattern, indicating synchronized heave motion behavior across the modules in long waves. Notably, the behavior remained unchanged before and after the breakage of the selected rope, suggesting that rope tension is not the primary factor governing the heave motion. The surge motions of the modules in line with the broken rope are shown in

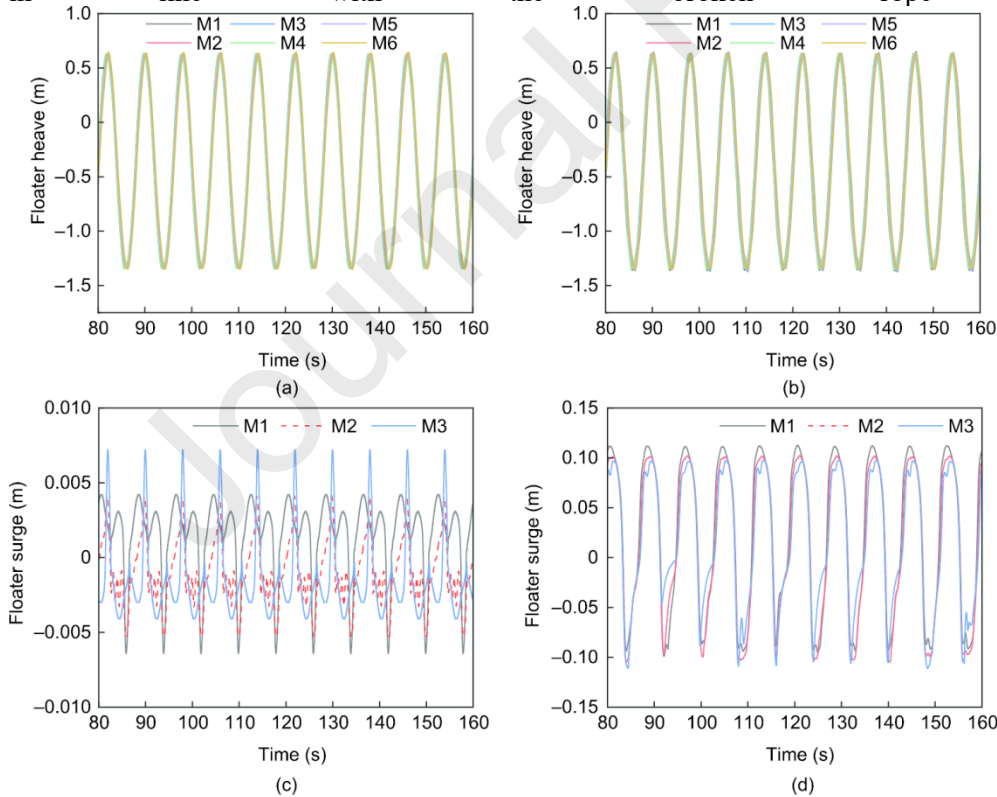
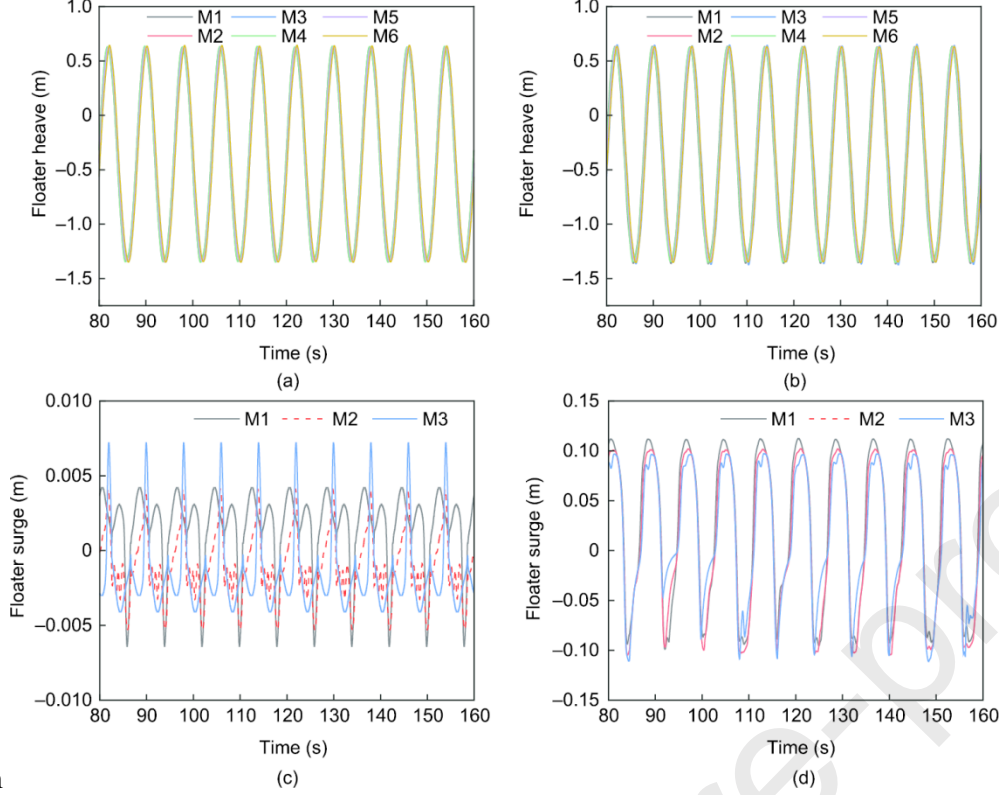


Fig. 17(c) and (d). A clear phase difference was observed between the modules: Modules 1 and 3 tended to oscillate in opposite phases, whereas Module 2 oscillated between them. Following the loss of tension in the selected rope, as shown



in

Fig. 17(d), significant amplification of surge motion occurs in Modules 1, 2, and 3. This increased motion resulted in the subsequent failure of other horizontal ropes in the same line, after which the modules' surge motions became synchronized in phase.

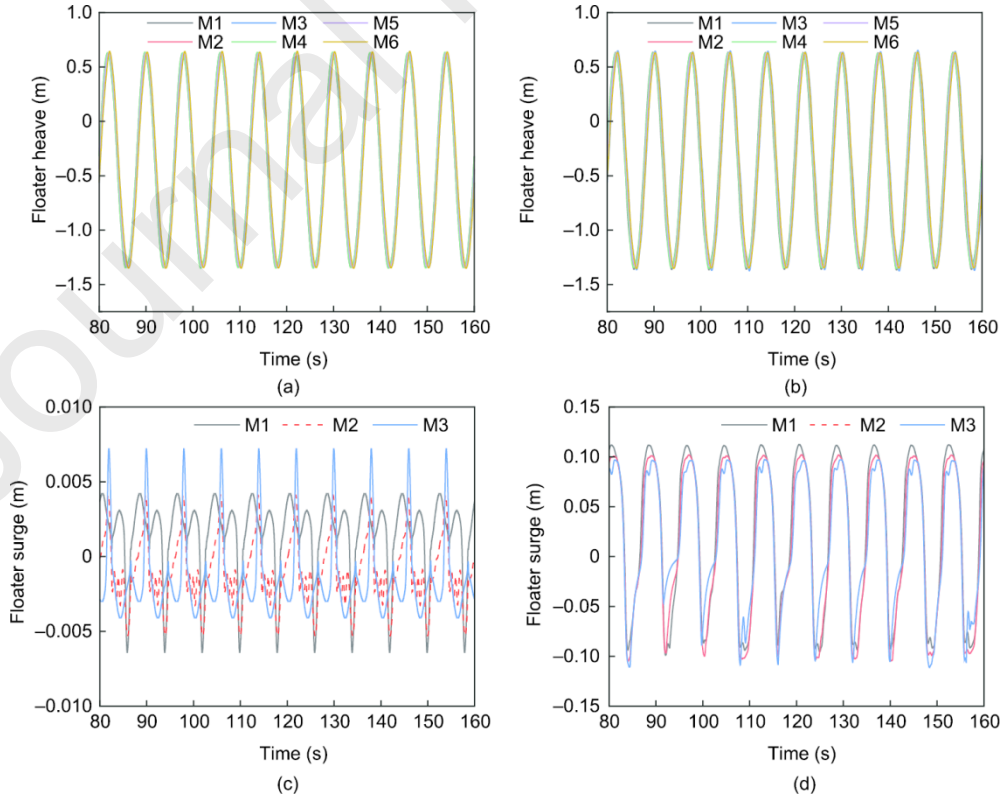


Fig. 17. Variations in module motions: heave motions (a) before and (b) after the selected rope break; surge motions (c) before and (d) after rope break.

Although these results demonstrate promising behavior at the module level, extrapolation to large-scale floating systems remains hypothetical. Further research is required to validate the structural and hydrodynamic performance of the scale.

6. Conclusions

This study introduced a novel web-type configuration for FPV systems, emphasizing the dynamics of multiple modules interconnected by elastic ropes. The impact of different array configurations on system performance under various open sea conditions was analyzed using the Morison model. The proposed solar web offers two key advantages: ① The flexible web frame enabled the system to deform collectively with wave action, effectively absorbing wave energy through the elastic deformation of the system and preventing snap loads in the waves. This advantage has been substantiated by the motion responses observed in our 3×1 module arrays. The results show that the phase angle between adjacent modules was minimal, suggesting negligible relative motion between neighboring modules. This implies that the system deforms coherently with the incident waves. ② The web framework demonstrated robust operational integrity even after the failure of one or more ropes, mirroring the resilience observed in natural spider webs. This characteristic has been validated in Section 5.2.2, using a 3×3 array.

For the 1×1 , 2×1 , and 3×1 module configurations, both motion and rope tension responses demonstrated similar performances with varying wavelengths. When the rope tensions were uniformly distributed, they effectively prevented collisions between modules. However, the shorter rope lengths in the 3×1 array introduced a higher risk of failure. Conversely, the tension performance was enhanced under non-uniform tension distribution with reduced gaps between the modules. Additionally, the implementation of pretension has proven to be an effective measure for mitigating substantial tension fluctuations, commonly referred to as “snap loads.”

In the 3×3 module array, tension distribution was analyzed under various wave directions and scenarios involving rope failure. When the wave propagation direction was aligned parallel to any of the ropes, the resulting tension distribution became highly unbalanced, substantially increasing the failure risk. In such scenarios, the failure of any horizontal rope (aligned with the wave direction) can precipitate catastrophic failure of the entire line and cause a significant increase in tension in the vertical ropes (perpendicular to the wave direction).

A limitation of the present research is the omission of hydrodynamic interactions between floating modules. The current simplified Morison approach could be enhanced by incorporating a wave transmission model, wherein the FPV farm is represented as a flexible porous plate [33]. This would allow for the attenuation of incoming wave amplitudes as they propagate through the FPV farm. Additionally, future research will extend the numerical analysis to larger configurations. The application of potential flow theory is also under consideration to more accurately simulate the dynamics of the floating multi-body system. The radiation interaction must be simplified by introducing a truncation scheme [34] to quantify the interaction distance. In addition, this study primarily addressed the fundamental mechanical behavior of local modules, with the global structural topology envisioned as an assembly of these local modules. While system integration and maintenance are beyond the current scope, they represent important directions for future investigation. The proposed structure is an early-stage concept for a modular, flexible system. The study results provide preliminary insights; however, comprehensive system-scale validation under realistic WCs is still required.

Acknowledgment

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Compliance with ethical guidelines

Zhi-Ming Yuan, Shuang-Rui Yu, and Atilla Incecik declare that they have no conflicts of interest or financial conflicts to disclose.

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Declaration of interests

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.